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The Proceedings

OF

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PART A

POWER ENGINEERING

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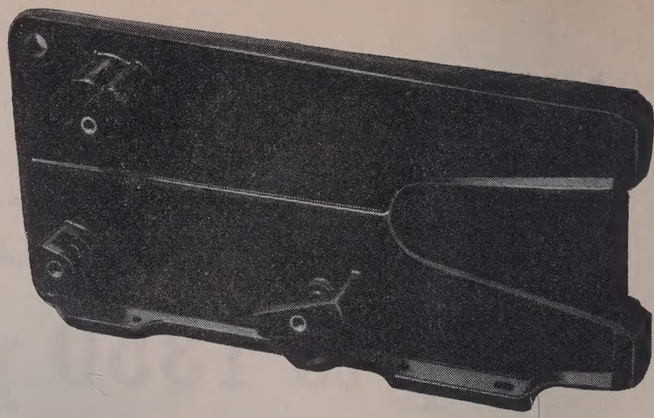

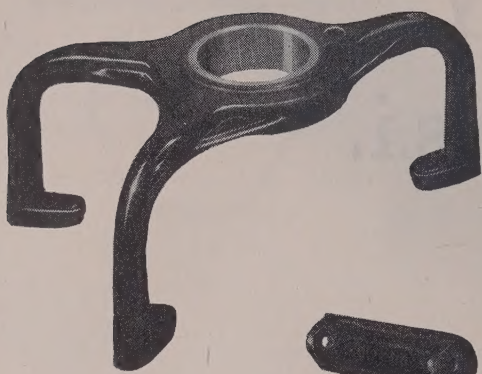

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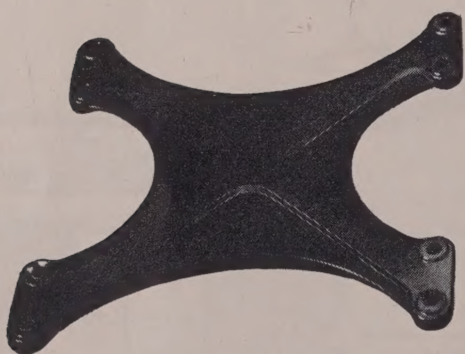

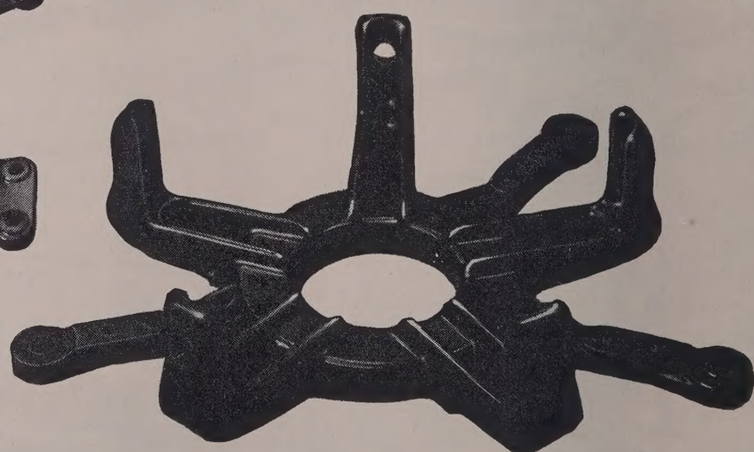

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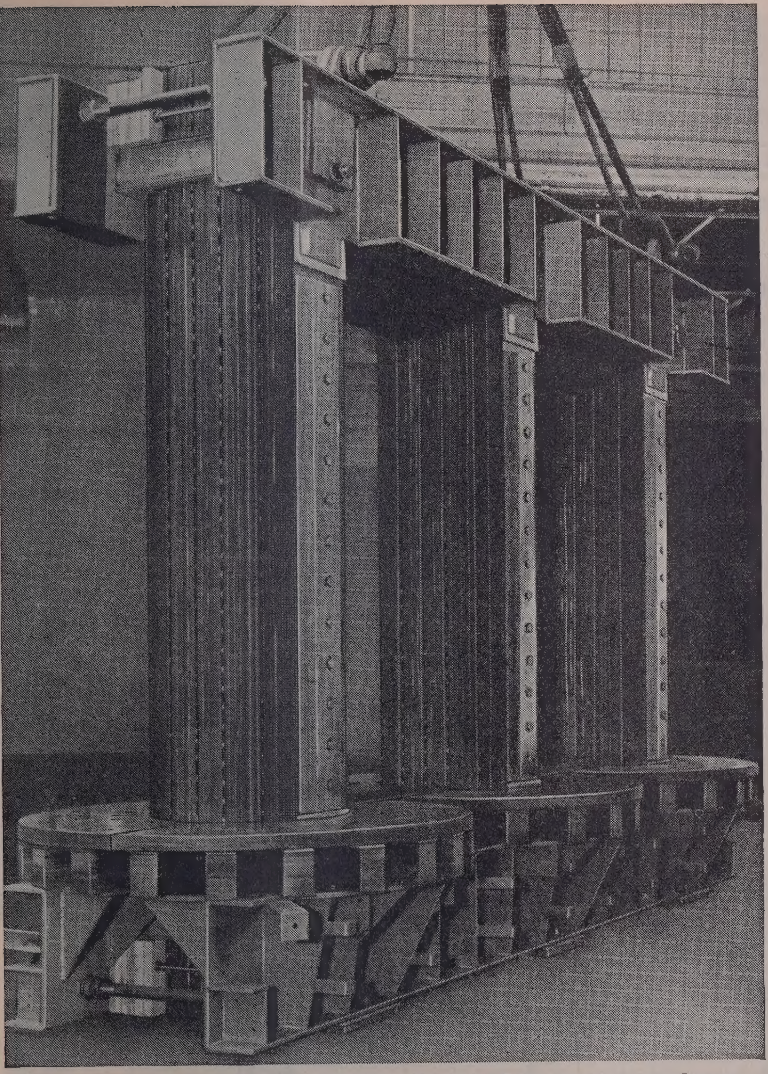
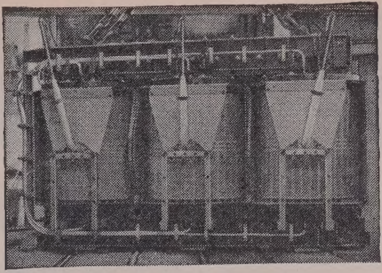
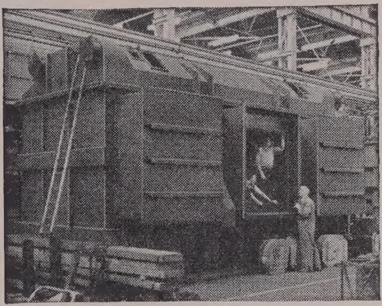


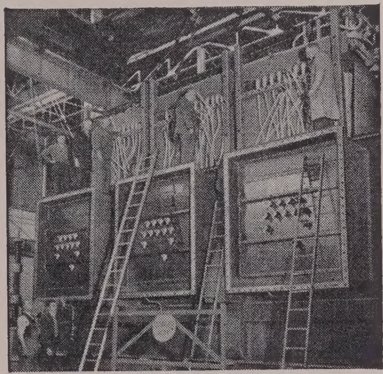
The finished core assembly of a 180 MVA 275/132 kV Hackbridge transformer showing the coil supports in position on the bottom clamping frames.



Core and coil assembly.

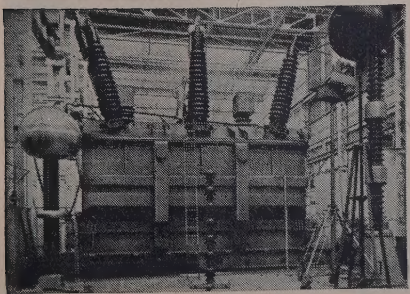


Preparation of tank.

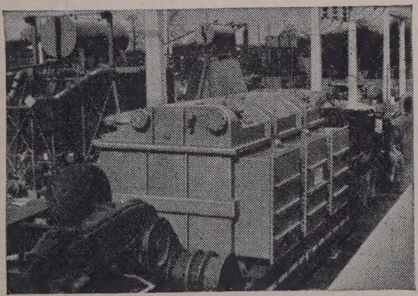


Lowering of core and coil assembly into tank.

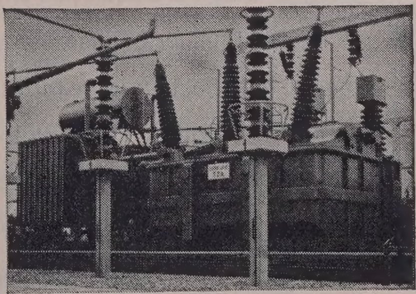
The Transformer Story



Following completion of tests in the Company's impulse testing laboratory.



Delivery to Central Electricity Generating Board super grid substation at Fleet, Hampshire.



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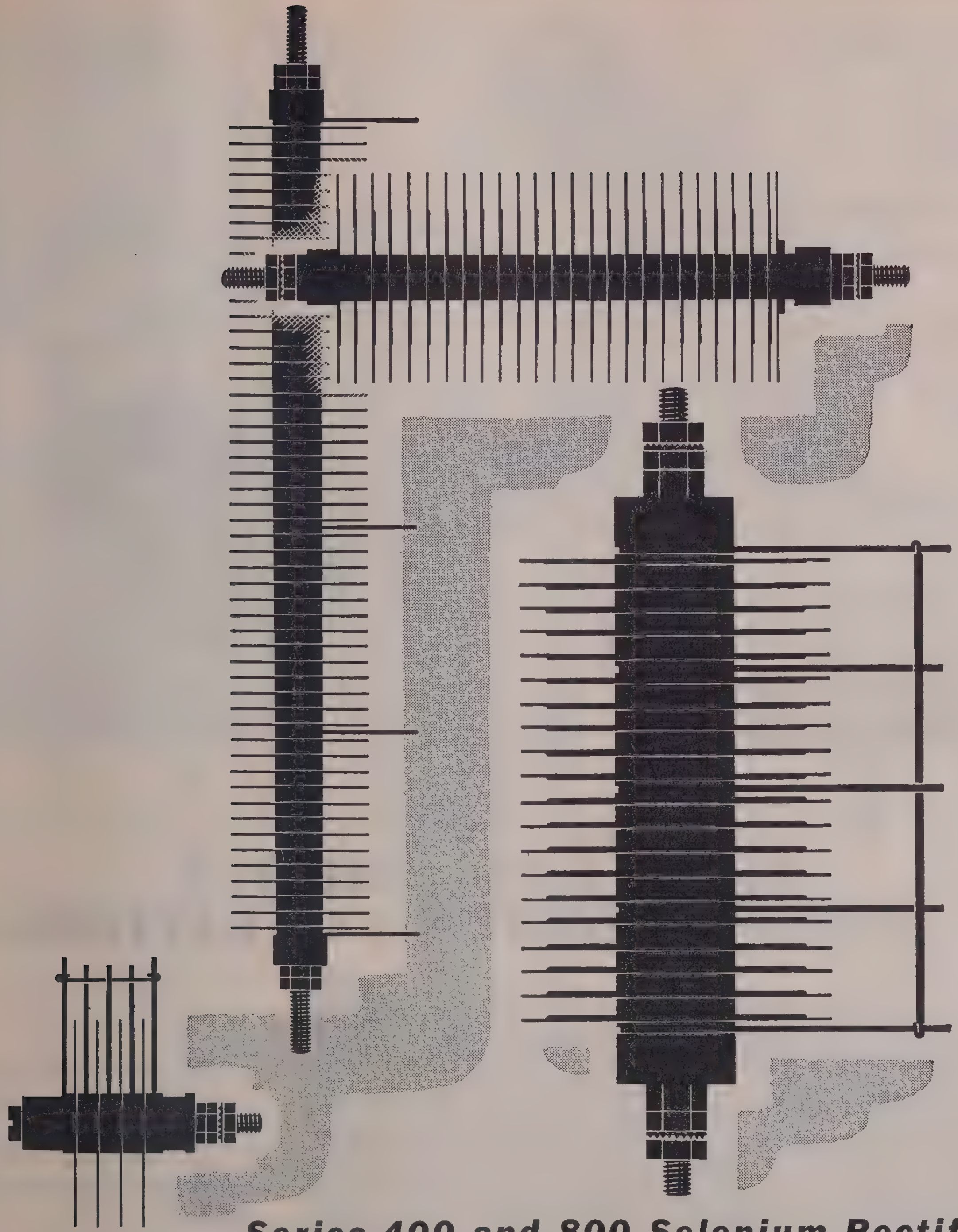

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33 H	3·1	3·3	3·5	90	100	250	600	50
36 H	3·4	3·6	3·8	86	95	80	200	47
39 H	3·7	3·9	4·1	80	90	35	100	44
43 H	4·0	4·3	4·5	72	85	13	50	42
47 H	4·4	4·7	5·0	62	80	3·0	30	40
51 H	4·8	5·1	5·4	50	70	1·0	30	38
56 H	5·3	5·6	6·0	28	50	0·6	10	35
62 H	5·8	6·2	6·6	10	30	0·4	10	33
68 H	6·4	6·8	7·2	3·7	15	0·85	10	29
75 H	7·1	7·5	7·9	4·0	15	1·25	10	27
82 H	7·7	8·2	8·7	5·5	20	1·5	10	25
91 H	8·6	9·1	9·6	8·0	20	2·0	10	23
00 H	9·4	10·0	10·6	11·0	30	2·0	10	21

	Forward volt Drop at 5 mA			Typical forward Slope resistance at 5 mA	Capacitance at −2 volts			Maximum Continuous forward current at 25°C mA
	Min. V	Typ. V	Max. V	ohms	Min. pF	Typ. pF	Max. pF	
types	0·67	0·71	0·73	6·3	100	275	700	225

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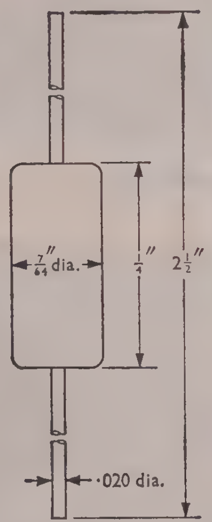
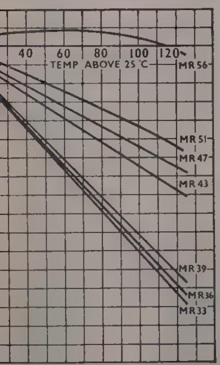
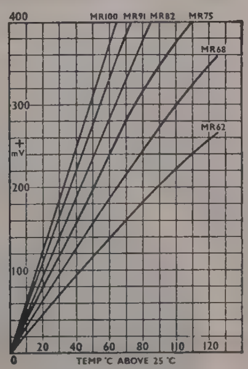




Typical change in erence voltage at 5 mA with temperature



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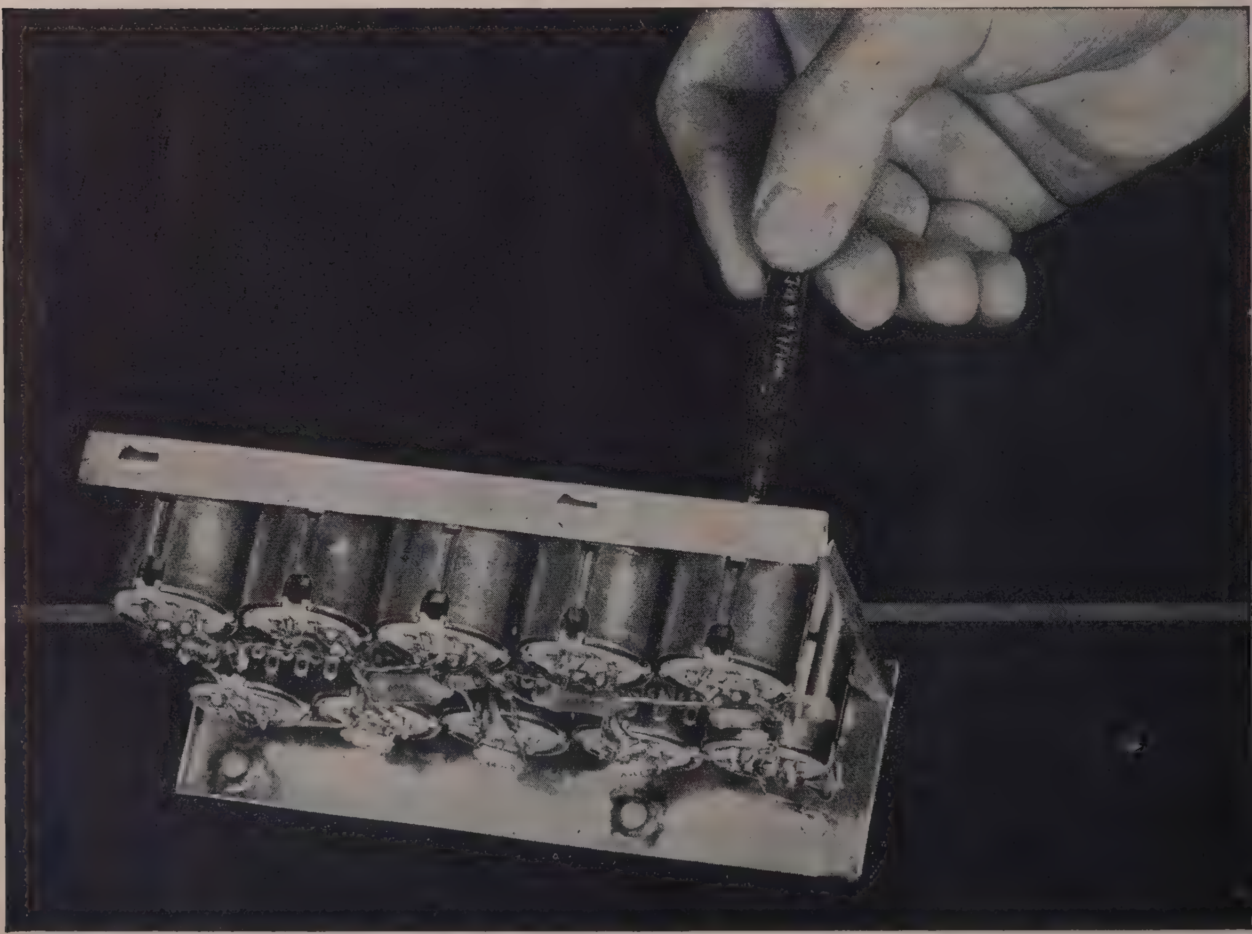

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SILICON POWER RECTIFIERS

By A. J. BLUNDELL, Associate Member, A. E. GARSIDE, B.Sc.(Eng.), Graduate, R. G. HIBBERD, B.Sc., Associate Member, and I. WILLIAMS, B.Sc., Associate Member.

(*The paper was first received* 6*th April, and in revised form* 12*th July*, 1960. *It was published in November*, 1960, *and was read before* THE INSTITUTION 2*nd February*, *the* NORTH-WESTERN CENTRE 14*th February*, *the* WESTERN CENTRE 13*th March*, *the* EAST MIDLAND ELECTRONICS AND COMMUNICATIONS GROUP 21*st March*, *the* SHEFFIELD SUB-CENTRE 19*th April*, *the* SOUTH MIDLAND CENTRE 1*st May*, *and the* NORTH STAFFORDSHIRE SUB-CENTRE 15*th May*, 1961.)

SUMMARY

The silicon rectifier is now well established over a wide range of voltages and currents, and, in all probability, will remain as a standard class for many years to come.

The paper opens with a brief survey of the processes involved in the preparation of single-crystal silicon; this is followed by sections devoted to design considerations and process techniques used in the preparation of silicon rectifier cells.

The electrical characteristics and ratings of rectifier cells, and the considerations involved in their operation in rectifying equipments, are discussed in some detail; brief mention is made of the various fields of application in which silicon rectifiers will offer advantage.

The latest device of this class—the silicon controlled rectifier—is described, and its importance in the future is emphasized.

Several theoretical aspects of the forward and reverse characteristics of silicon rectifier cells are treated in the Appendices.

LIST OF SYMBOLS

A = Area.
C = Capacitance.
D_s = Diffusion constant in the s-region.
$D = w/2$ = Half the width of the base region.
e = Electronic charge.
W_c = Lowest energy level in the conduction band.
W_r = Energy level of a recombination centre of type (i).
W_v = Highest energy level in the valence band.
G_{th} = Effective rate of thermal generation of electrons or holes.
g_{th} = Effective rate of thermal generation of electrons or holes per cubic centimetre.
I = Current.
I_r = Reverse leakage current.
I_m = Mean forward current.
I_{c0} = Collector current with no injection.
J = Current density.
K = Thermal conductivity.
k = Boltzmann's constant.
k_{et} = Mass-action constant for the reaction between free electrons and recombination centres of type (i).
k_{hj} = Mass-action constant for the reaction between free holes and recombination centres of type (i) (when dealing with non-degenerate hole concentrations, k_{ht} is often quoted as 'the hole concentration for the case when the Fermi level and the centre level coincide').
l = Width of space-charge region.
L_s = Diffusion length in the s-region.
N_c = Effective density of states in the conduction band.
N_v = Effective density of states in the valence band.
n^+ = n-type semiconductor which has a very high equilibrium concentration of electrons when electrically neutral.
n_e = Concentration of electrons.
n_h = Concentration of holes.
$\bar{n}_i$ = Equilibrium concentration of free electrons (or free holes) in a pure and neutral semiconductor.
n_{tj} = Concentration of recombination centres of type (i).
p^+ = p-type semiconductor with a very high equilibrium hole concentration when electrically neutral.
r = Junction radius.
s = s-type semiconductor with a low equilibrium electron and hole concentration when electrically neutral.
T = Absolute temperature.
$\theta_{j\,max}$ = Maximum junction temperature.
θ_{ca} = Temperature of cooling air.
V_r = Peak reverse voltage.
V_B = Avalanche breakdown voltage.
V_f = Peak forward voltage.
V_s = Voltage drop over s-region.
P_i = Total internal power loss.
P_f = Forward loss.
w = Width of base layer.

Messrs. Blundell, Garside and Williams are with Associated Electrical Industries, Ltd.
Mr. Hibberd was formerly with Associated Electrical Industries, Ltd., and is now with Texas Instruments, Ltd.

α = Current gain in a transistor (common base).
e_j = Recombination constant for the recombination of free electrons with centres of type (i).
h_j = Recombination constant for the recombination of free holes with centres of type (i).
S_h = Thermal resistance of the cooling fin to the cooling air.
S_i = Internal thermal resistance, junction to case, of the rectifier cell.
ρ_n = Resistivity of *n*-type material.
ρ_p = Resistivity of *p*-type material.

(1) INTRODUCTION

During the last 40 years, there has been a succession of solid-state power rectifiers—copper-oxide, selenium, germanium and now silicon. Each of these in turn has brought distinct improvements in performance, size and weight, particularly in peak reverse voltage and kilowatt output per unit volume.

Copper-oxide rectifiers had a reverse-voltage rating of 6–12 volts per plate and a forward-voltage drop of 0·3 volt with a current density considerably less than 1 amp/cm^2. Selenium gave a significant improvement in reverse voltage with 30–40 volts per plate; the forward voltage drop was higher (0·7 volt), but as fewer series plates were needed the overall drop for a high-voltage rectifier was similar. After the selenium rectifier had been established for some 20 years a new class of rectifier, based on the *p–n* junction, made its appearance with greatly improved performance and smaller physical size. The first of this new class was the germanium rectifier,[1] which is now marketed for crest working voltages of up to 300 volts, and for peak transient reverse voltages of up to 600 volts. It has a forward voltage drop of 0·5 volt, and a maximum operating junction temperature in the range 75–90° C. This was followed by the silicon rectifier, which is now well established. Silicon rectifiers are at present marketed for crest working voltages up to 600 volts and transient peak reverse voltages up to 1·2 kV. The forward voltage drop is about 1·1 volts, and the maximum operating junction temperature varies in different designs from 120° C to 200° C.

Because of the small physical size of silicon rectifier cells, their thermal capacity is low, necessitating careful design for overload conditions.

A more recent development is the silicon controlled rectifier, which in many ways is analogous to the thyratron, and which promises to be of great importance in industrial applications. A 10 amp unit is now available for voltages up to 300 volts, and higher-current types are under development.

(2) SILICON PROCESSING (Reference 2)

It is now considered axiomatic that the starting-point for a discussion on semiconductor rectifiers is the preparation of large single crystals of extremely pure semiconductor material. Purity in this context implies impurity concentrations of less than one part in 10^9, and, in producing silicon of this quality, chemical engineers have contributed in no small part to the successful introduction of silicon rectifiers.

The history of rectification using silicon started seriously in 1940 when silicon diodes were developed for use in radar. The silicon was prepared by the reduction of silicon tetrachloride by zinc vapour at 900° C, and was very impure compared with present standards. More recent methods of large-scale production involve the reduction or thermal dissociation of silicon halides or hydrides:

$$SiCl_4 + 2H_2 \rightarrow Si + 4HCl \quad . \quad . \quad . \quad . \quad (1)$$

$$4SiHCl_3 \rightarrow Si + 3SiCl_4 + 2H_2 \quad . \quad . \quad (2)$$

$$SiI_4 \rightarrow Si + 2I_2 \quad . \quad . \quad . \quad . \quad . \quad (3)$$

$$SiH_4 \rightarrow Si + 2H_2 \quad . \quad . \quad . \quad . \quad . \quad (4)$$

The halide is purified by fractional distillation, and its vapou diluted with the requisite amount of purified hydrogen as carrier gas, is passed into the reaction chamber. At approx mately 1 000° C the halide decomposes and silicon is forme In one technique the silicon is deposited on a fine silicon roc some of the purest silicon has been prepared in this wa Method 4 has the advantage that the reactants, being gases, a more readily purified. It has a disadvantage in that the hydric silane is spontaneously explosive on exposure to air.

It is estimated that the raw material being prepared com mercially contains no more impurities than one atom in 10^9.

This silicon is polycrystalline and must be converted int single-crystal form. Also, the requisite minute amount of suitable impurity must be introduced in order to obtain th required resistivity, namely phosphorus for *n*-type crystals an boron for *p*-type.

A method often used for single-crystal growing is th Czochralski technique. Silicon, together with the dopin impurity, is contained in a crucible and heated to a temperatur just above its melting point. A single crystal seed, in a holde is lowered into the melt and complete wetting of the end of th seed by the molten silicon takes place. The temperature of th melt is slowly decreased, and silicon grows epitaxially on to th seed. By raising the holder the silicon in the crucible grow as a single crystal, on to the seed.

The technique suffers from the limitation that molten silicon extremely reactive, and no entirely satisfactory crucible materi has been found. The one most widely used is fused quartz.

A preferred method of crystal growth at present being widel used is a development of the floating-zone technique first use by Keck. A long bar of silicon is clamped by its ends in vertical position, and a narrow molten zone is produced b r.f. induction. If now the zone is repeatedly passed up the ba impurities will be moved to the top by the process of zon refining, without the danger of contamination from crucible c boat materials. If a single crystal seed is positioned at th bottom and the molten zone passed from it to the polycrystallin bar, the latter will be converted to single-crystal form simu taneously with the refining.

Since the bulk resistivity of the single-crystals is controlled b the addition of minute quantities of specific impurities, extrem care is necessary in handling the silicon, and this should preferab be carried out in a controlled atmosphere. The reagents use for cleaning crystals and chemical ware contain hydrofluoric an nitric acids, and high-purity 'transistor' grades have bee developed for use in semiconductor processing. Materials an apparatus are washed in running deionized water, the effluer being monitored until its conductivity has returned to the origin value of that of the deionized water. Contamination fro metallic ions is avoided by the use of polythene or fluon war

(3) SILICON MATERIAL AND WAFER DESIGN

Fig. 1 illustrates the basic arrangement of a *p–n* junctic rectifier element. A wafer of *n*-type silicon in which a *p*- junction has been formed is assembled to a heat sink and a to electrode with ohmic contacts. A typical characteristic is als shown.

In starting the development of a silicon rectifier, it is fir necessary to determine the requirements of the silicon wafer.

(3.1) Resistivity and Reverse Voltage

The parameters of interest in the reverse characteristic a the maximum voltage and the saturation current. The max

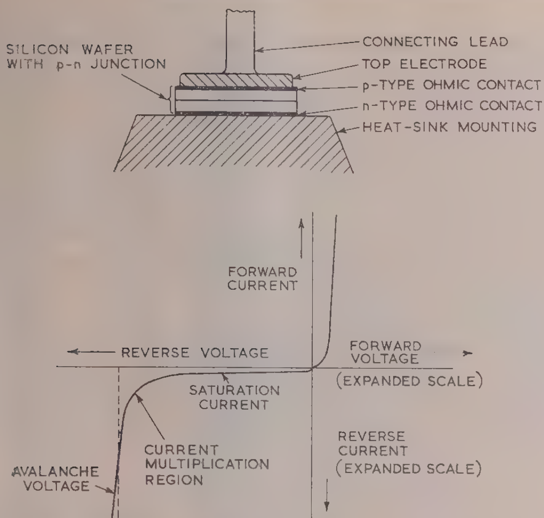


Fig. 1.—Basic *p–n* junction rectifier and characteristic.

num reverse voltage is determined by the onset of current multiplication. Relations between silicon resistivity and reverse avalanche voltage (Section 13.1) indicate that the higher the esistivity, the higher is the avalanche voltage. For instance, vith *n*-type silicon, 1 ohm-cm will give 48 volts; 10 ohm-cm, 270 volts; and 100 ohm-cm, 1 500 volts. However, for other easons there is an upper limit to the resistivity which may be ised. First, the reverse saturation current is partly dependent on esistivity (Section 13.2), and too high a resistivity may result in in excessive reverse current at high temperature; the maximum everse working voltage will then be limited by excessive reverse power dissipation. Secondly, under certain conditions a high esistivity will adversely effect the forward characteristic and give oo high a series ohmic resistance of the junction. In practice, or high-voltage *n*-type rectifiers suitable for 200° C operation, a esistivity about 70 ohm-cm is optimum, and can yield a large proportion of units which will withstand a test voltage of 1·2 kV. With *p*-type silicon, the use of 1 kilohm-cm has been reported o give units withstanding test voltages up to 1·2 kV at 150° C.

(3.2) Lifetime of Minority Carriers, Wafer Thickness and Forward Voltage Drop

In order to obtain a low forward voltage drop in the rectifier, he central base region must be less than the diffusion length of he injected carriers (Section 13.3). However, if this region is nade too thin the reverse breakdown voltage will be limited owing to 'punch through' (Section 13.1). A typical minority-carrier lifetime after junction formation is of the order of 5 microsec, corresponding to a diffusion length of holes in *n*-type silicon of 0·003 in. Thus the thickness of the central base egion must be of this order. Taking into account the penetra-ion during junction and ohmic-contact formation, a starting vafer thickness of 0·008 to 0·010 in is required. To allow for he removal of material by lapping and etching, the slices should be cut 0·014 to 0·018 in thick.

(3.3) Junction Area

The required junction area of a silicon rectifier depends upon he allowable internal temperature rise with full-load current lowing. A knowledge of the approximate forward voltage drop will allow the internal power loss to be calculated, and con-ideration of the flow of the resulting heat away from the junction will give an approximate area for a stated maximum junction temperature rise. The final current rating of a rectifier may, however, depend upon other factors, such as the short-circuit performance.

It can be shown[3] that a rectifier mounted on a flat surface of an infinitely large block has a temperature rise, $\Delta\theta$, above the block, given by

$$\Delta\theta = \frac{rJV}{K} \quad . \quad . \quad . \quad . \quad . \quad . \quad (5)$$

This indicates that, for a given temperature rise, the current density must be decreased as the junction area is increased. In practical rectifiers the current density is decreased from the order of 500 amp/cm^2 for a small-area (1 amp) type to about 80 amp/cm^2 for a large-area (200 amp) type.

(4) RECTIFIER CELL PREPARATION

(4.1) Basic Cell Arrangement

A typical arrangement of a silicon rectifier cell is illustrated in Fig. 2. The silicon wafer, processed to include the *p–n*

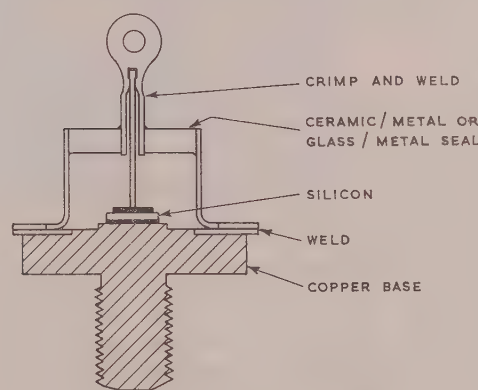


Fig. 2.—Basic silicon rectifier cell encapsulation.

junction, is mounted on to a copper base. The top connection is taken out through an insulated tube in the main body, and the cell is hermetically sealed.

(4.2) *p–n* Junction Formation

Two basic methods of junction formation, alloy and diffusion, are suitable for the manufacture of silicon power rectifiers. Each of these processes has its particular advantages and dis-advantages.

(4.2.1) Alloyed Junctions.

For silicon rectifiers, it is usual to alloy aluminium to *n*-type silicon, or gold-antimony to *p*-type silicon. For small-area junctions, suitable for currents up to about 1 amp, one end of an aluminium wire can be alloyed to the silicon; the wire then forms a very convenient means of connection. For medium- and large-area junctions, it is usual to use thin foil which is alloyed to the silicon. As the area is increased, however, uni-form wetting of the silicon surface by the foil becomes difficult, necessitating very careful control of the flatness of the silicon wafer and foil through the various processes. An alternative method is to evaporate on to the silicon surface a thin layer of aluminium which makes intimate contact with the silicon and gives very reliable wetting, resulting in good control of the subsequent alloying. Junctions with excellent forward and reverse characteristics can be produced by this evaporation alloy method.

(4.2.2) **Diffused Junctions.**

From several points of view, the more recently developed diffusion process is a very attractive approach to junction formation. First, the positioning and characteristics of the junction should be more readily controlled. Secondly, it offers the possibility of a common operation for a range of rectifiers, since the diffused wafers may be diced into small or large squares as required.

To produce a diffused junction, a suitable impurity is diffused into the solid silicon by heating it in the presence of the impurity for a prolonged period at a temperature of about 1 250° C. For example, phosphorus can be diffused into *p*-type silicon to give an *n*-type layer 0·000 1 in-thick by heating the silicon at 1 200° C for 15 hours in an atmosphere containing phosphorus pentoxide.

One of the limitations at present is that severe degradation of the minority-carrier lifetime takes place during any heat cycle. In this respect, the diffusion process is more drastic; a shorter lifetime is obtained after diffusion, and consequently a thinner base region is needed to obtain a good forward characteristic. This may then impose a limitation on reverse voltage owing to 'punch through'. Methods of preserving the lifetime, e.g. by the use of nickel, show promise and are being actively investigated.

(4.2.3) **Relative Merits of Alloyed and Diffused Junctions.**

Whichever method is used, some aspects of control are identical. To obtain uniform forward characteristics, it is necessary to control both the silicon wafer thickness and the position of the *p–n* junction to within a fraction of 0·001 in. To maintain a good reverse characteristic, assuming that the quality of the starting silicon is consistently good, important factors are the use of very pure auxiliary materials and meticulous cleanliness in all operations from the wafer lapping through to the encapsulated cell. While more nearly ideal junctions with better control may eventually be made using the diffusion process, the resulting rectifier cells may be more costly than those made using the alloy process since the latter need fewer steps in fabrication.

From the performance viewpoint there appears to be little to choose between the two methods, and manufacturers who started production using the earlier alloy process can see no reason to change.

(4.3) Ohmic Contact Formation

In addition to producing the *p–n* junction in the wafer of silicon, it is necessary to make good ohmic contacts to the *p* and *n* regions. These must have a very low ohmic resistance, which should be negligible compared with the series resistive component associated with the actual *p–n* structure. At least one of these ohmic contacts should have a very low thermal resistance, so that the heat generated in the device can readily flow away.

With the alloy process, the ohmic contacts are usually made at the same time as the junction. One typical arrangement for

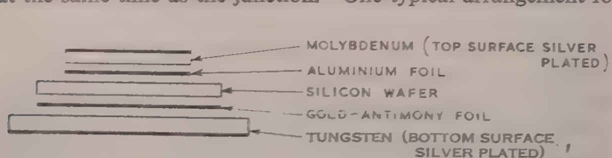


Fig. 3.—Alloyed rectifier arrangement.

a large-area rectifier is shown in Fig. 3. The constituent parts, tungsten base electrode, gold-antimony foil, *n*-type silicon wafer, aluminium foil and molybdenum top electrode are stacked in a suitable jig, and passed through a carefully controlled heat cycle. During this cycle the aluminium foil alloys to the top surface of the silicon to form the *p–n* junction, the gold-antimony foil alloys to the bottom of the silicon to form the ohmic base contact, and each foil bonds to its electrode.

With *p*-type rectifiers, the fabrication procedure is similar, but the roles of aluminium and gold-antimony are reversed.

In order to reduce the thermal resistance, it is preferable to avoid the use of electrode discs, and in this connection the evaporation alloy process gives a structure which can be readily assembled by soldering the aluminium or gold-antimony surface direct to a copper base.

The problem of making soldered ohmic contact to diffused wafers is somewhat more involved, since diffused surfaces are more difficult to wet, and care must be taken to limit the contact penetration so that the junction characteristics are not affected. A technique widely used is to electroless-nickel-plate the diffused surfaces and to make soldered connection to the nickel.

(4.4) Encapsulation

Most of the reverse current of a silicon junction flows through a surface layer which is extremely sensitive to contamination. It is essential to remove all contaminants by suitable cleaning and washing processes, and then to dry and finally seal the container. The seal is either a soldered seal or a weld between two metal surfaces, projection welding, argon arc welding and cold pressure welding all being used. The requirements mentioned earlier for the ohmic contacts apply equally to the internal connectors from the basic wafer to the output terminals, namely low electrical resistance and a low thermal resistance path to an external surface suitable for thermal connection to a fin system. The encapsulation must obviously be strong and robust, particularly for the high-power cells, to which high-current cables must be connected. Insulation between the electrical output terminals is by glass/metal or ceramic/metal assemblies.

Low- and medium-current silicon rectifiers, for stud mounting on to a fin, are generally encapsulated as indicated in Figs. 4(*c*) to (*f*). The copper stud forms both one electrical and the thermal contact. For high-current types it may be preferable to separate the electrical and thermal connections, since the electrical contact needs a very high pressure, conveniently given by a small contact area, while the thermal contact needs a moderate pressure but a large surface area to give a low temperature drop.

A somewhat unconventional enclosure used for one high-current cell is shown in Fig. 4(*h*). It has a large rectangular copper base to spread the heat over the thermal contact, and the electrical connections are made by flexible leads.

(5) RECTIFIER PROPERTIES

Silicon has an energy gap of 1·08 eV compared with 0·68 eV for germanium. As a result, intrinsic silicon must be taken to a higher temperature to give a resistivity similar to that of intrinsic germanium. For instance, if the temperature of germanium is taken as 75° C, the corresponding temperature of silicon to give the same resistivity will be about 250° C. Thus these can be considered as equivalent temperatures.

(5.1) Reverse Characteristics

If silicon and germanium rectifiers are compared at their equivalent temperatures, the ideal characteristics are similar. At present it is not possible to make silicon rectifiers with reverse characteristics at 250° C as good as those of germanium rectifiers at 75° C, mainly owing to surface effects. However, it is possible to take advantage of the higher equivalent temperature of silicon

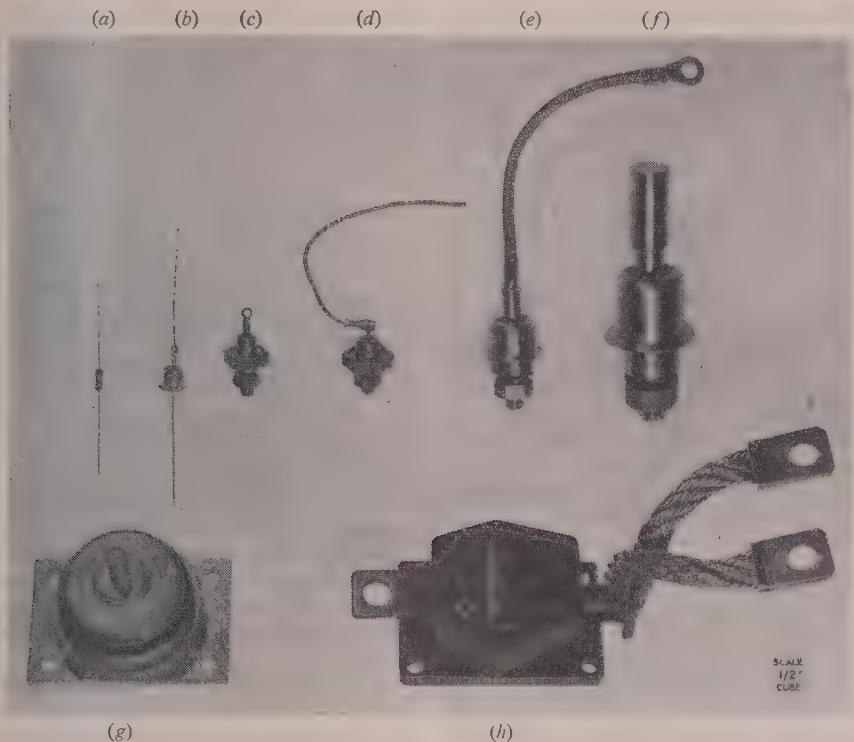


Fig. 4.—Range of silicon rectifier cells.

(a) Miniature wire-ended type.
(b) Low-current (1amp) wire-ended type.
(c) Low-current stud-mounted type.
(d) 10 amp cell.
(e) 30 amp cell.
(f) 60 amp cell.
(g) 100 amp cell for aircraft applications.
(h) 200 amp cell for industrial use.

by limiting the operating junction temperature to, say, 150° C, thus reducing the reverse leakage current and allowing a higher voltage for the same reverse power loss. By this means it is possible to use silicon at higher voltage ratings than germanium with normal ambient temperatures. (With a silicon junction at 150° C, the equivalent junction temperature of germanium is 7° C.)

The representative curve (i) of Fig. 5 illustrates the points of interest in the reverse characteristic, and is valid for different sizes of rectifier. Region A indicates that the reverse current saturates for voltages greater than about ¼ volt, and for an ideal rectifier would remain constant for several hundred volts. Usually a voltage-dependent component appears in the region B owing to generation within the space-charge region. At higher voltages the field strength increases sufficiently to cause large currents by impact ionization, leading to avalanching, which gives the soft region C and the voltage limiting region D. The voltage dependence of these currents can be complicated. Conditions at the silicon junction surface may cause large contributions to the total reverse current, and the field strength in the surface layers can be higher than those in the bulk. The characteristic will be quite stable, provided that there is no active contamination on the surface. It should be noted that owing to surface effects the reverse power dissipation is more localized at the periphery of the junction, and allowance must be made for this when rating the rectifier.

In the above discussion the use of the word 'breakdown' has been deliberately avoided when referring to high field effects, because these do not cause breakdown in a destructive sense.

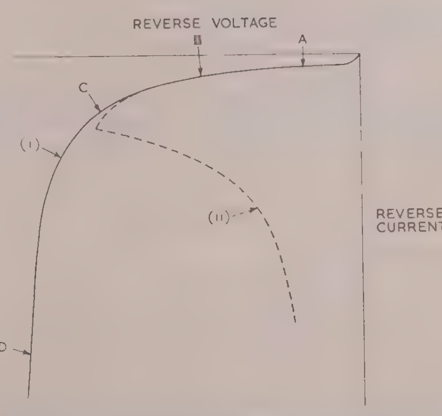


Fig. 5.—Representative reverse characteristics.

The term 'avalanche breakdown', often used in this context, denotes quite a stable condition and does not imply a hazard, but more a limit of operation. The term 'breakdown' is more correctly applied to a negative-resistance condition, caused by local imperfections. This type of breakdown has been discussed by Tauc and Abraham,[4] who attribute it to a thermal triggering effect. A typical characteristic is shown in curve (ii) of Fig. 5. All operational failures of semiconductor rectifiers, except those due to overheating, can be attributed to this negative-resistance type of breakdown.

(5.2) Forward Characteristics

At the equivalent working temperatures, the forward characteristic of a silicon rectifier is similar to that of a germanium rectifier for current densities less than 100 amp/cm^2. In both cases there are two components—an exponential component with a negative temperature coefficient and a resistive component with a positive temperature coefficient. The resistive component becomes appreciable above 100 amp/cm^2, and with practical silicon rectifiers at the present time it appears to be larger than in the corresponding germanium rectifiers. Thus above 100 amp/cm^2 the forward voltage drop with silicon is somewhat higher than with germanium at the equivalent temperature. If the temperature of a silicon rectifier is reduced to 150° C, in order to obtain the advantage of high reverse voltage, the negative temperature coefficient of the exponential component causes the voltage drop to be higher still (about 1·1 volts), which is about twice that of a germanium rectifier at 75° C.

Earlier in the paper, a typical current density for large-area rectifiers was quoted at 80 amp/cm^2. This is the mean current density, and with 3-phase double-way operation the cell will be operating at 240 amp/cm^2 for one-third of the cycle. Thus the resistive component is important even at normal working currents.

At some current above the normal working value, the resistive term predominates, and the temperature coefficient of the forward voltage drop becomes positive. This condition will apply to current surges under fault conditions. Since, with very-high-current cells, the maximum operating current is often set by their ability to withstand a high fault current, the reduction of the resistive term to a minimum is of paramount importance. In a practical rectifier of 2 cm^2 junction area, the resistive term has a value of about 0·3 milliohm.

Some aspects of the forward characteristic of the silicon rectifier are further treated in Section 13.3.

(5.3) Minority-Carrier Storage

The hole storage effect is present in *n*-type silicon rectifiers to an extent depending on the hole lifetime in the base region. Since this lifetime is, in general, shorter than in germanium rectifiers, the hole-storage duration is correspondingly shorter. However, the voltage surge produced at the end of commutation by the rapid decay of the hole storage current is very undesirable, and must be reduced by connecting a capacitor across the circuit as described in a previous paper.[1]

(5.4) Rating and Test

(5.4.1) Junction Temperature.

The first consideration in the rating of a silicon rectifier is the maximum junction temperature. This may be chosen to give any desired compromise between ambient temperature, peak reverse voltage and maximum forward current. For instance, where high reverse voltage is required, a junction temperature of 120–150° C can be used. For lower voltage, higher current or high ambient-temperature applications, the junction temperature can be increased to the region of 200–250° C.

(5.4.2) Reverse Voltage Rating.

There are two factors to be considered when deciding on the reverse-voltage rating of a rectifier, thermal runaway and the negative-resistance type of breakdown described earlier. Thermal-runaway considerations, i.e. the ability of the cooling system to remove heat faster than it is being generated by the internal power loss, determine the maximum continuous working voltage. The negative-resistance breakdown, on the other hand, is initiated in a few microseconds, and thus does not depend on the duration of the applied voltage. The continuous working voltage and the momentary over-voltage ratings can therefore be regarded as independent.

For the purpose of defining the reverse voltage that may be allowed to appear across the rectifier cell under different conditions, the following terms are becoming widely used:

(*a*) *Crest working reverse voltage* (*c.w.v.*).—Crest value of the reverse voltage across the cell, which results from the applied voltage in rectifier duty, ignoring all voltage oscillations and transients.

(*b*) *Maximum recurrent reverse voltage* (*m.r.v.*).—Peak value of the reverse voltage, including the voltage oscillations which are repeated in every cycle.

(*c*) *Peak transient reverse voltage* (*p.t.v.*).—Highest reverse voltage that may occur owing to random voltage transients, even if of extremely short duration and occurring only once.

The crest working reverse voltage will depend mainly on the thermal-stability point. For a given reverse voltage and cooling system, the runaway condition depends mainly on the rate of increase of reverse current with temperature. This would be an inconvenient measurement in production. In practice, it is found that dI_r/dT is dependent upon the reverse current I_r, and so, if this current is limited by limiting the reverse power, the value of dI_r/dT is limited. Thus it is possible to use a power limit to determine the crest working reverse voltage.

The maximum recurrent and peak transient reverse voltages apply to recurrent or isolated surges and are determined by the negative-resistance breakdown. At present, they are not usually separated.

To illustrate these reverse ratings, a type SP20D30 rectifier (200 amp) has a crest working reverse voltage of 300 volts, and maximum recurrent and peak transient reverse voltages of 600 volts. Smaller rectifiers are used in a wide variety of applications, and at present it is considered desirable to have only one reverse voltage rating. Thus the crest working maximum recurrent and peak transient reverse voltages are identical. For example, the SJ401 rectifier (0·5 amp) has a single reverse rating of 400 volts.

(5.4.3) Reverse Voltage Test.

As indicated above, the usual method of testing for the crest working reverse voltage is to set a maximum limit of reverse power, or reverse current, at that voltage and maximum junction temperature. For a 200 amp rectifier, a reverse power limit at 150° C junction temperature may be several tens of watts peak. For a 1 amp rectifier at 200° C a 1 mA reverse current is typical. If a higher maximum recurrent or peak transient reverse voltage is required, the test voltage must be increased to this value to ensure freedom from breakdown. If the incidence of breakdown is encountered before the required value, the crest working reverse voltage must be decreased to a lower grade. The production test voltage is usually about 25% above the rated peak transient reverse voltage.

(5.4.4) Forward Current Rating.

The continuous forward current at which a rectifier cell can be operated depends upon the cooling system used. Thus the forward current rating of a rectifier cell can only be stated by quoting characteristics such as the following, from which the operating current for a given cooling system can be determined:

(i) Maximum junction temperature.
(ii) Forward characteristic.
(iii) Thermal resistance from junction to cell base.
(iv) Maximum r.m.s. current which is permitted by the allowable power loss in the internal connectors.

A current rating often quoted for the cell is the infinite heat-sink rating deduced from (i), (ii) and (iii). However, it should be remembered that the cell can only be operated near this

current by water-cooling the base. With convection cooling, a typical operating current will be about half the infinite heat-sink value. Item (iv) is important in high-power rectifiers.

For high-power rectifiers the maximum forward current is frequently set by the surge rating. This is so as, on the occurrence of a d.c. fault, the rectifier must safely pass the resulting high fault current until a protective circuit-breaker or fuse operates. The ratio of full-load to short-circuit current is set by the circuit constants, and this ratio determines the maximum continuous current when the surge rating is known.

(5.4.5) Surge-Current Rating.

The surge-current rating is of considerable importance in ensuring that the rectifier will withstand the hazards of certain applications. The value is a function of the heating effect of the surge, and so is related to the junction temperature immediately before the surge appears. Low- and high-current rectifiers usually encounter different conditions in service.

Low- and medium-current cells are rarely used in parallel, and a short-circuit on the d.c. side is usually interrupted by a fuse or a circuit-breaker on the a.c. side. In practically all cases, the short-circuit will remain until the fuse has cleared, and there will be no reverse voltage impressed across the cell after the fault current has stopped. Thus, for these cells, it is not necessary to apply reverse voltage during the surge-current test. Fig. 6 shows a surge-current rating for a typical 10 amp cell.

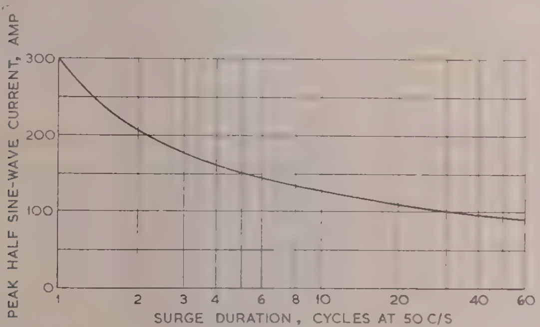


Fig. 6.—Surge-current rating for a typical 10 amp cell.

With high-current cells, parallel connection is often used, the parallel paths being separately fused. If a rectifier cell fails, e.g. owing to an external fault or over-voltage surge, its short-circuit current will pass through the cells in other arms of the bridge. When the fuse clears, the circuit isolates the faulty cell, and the full reverse voltage will appear across the other cells. Thus the surge-current rating for high-power cells must include the application of full reverse voltage immediately after the surge. It is found that the permissible surge current decreases with increase in reverse voltage, and so the full-load current rating will also decrease with increasing reverse-voltage rating.

(5.4.6) Forward-Current Tests.

The forward test usually consists in passing the peak full-load current through the cell and noting that the peak forward voltage drop is less than the maximum allowable value. Also, it is now considered advisable to carry out a thermal-resistance test on all factory-produced units. This involves the measurement of the junction-temperature rise for a given internal dissipation. One convenient method for making this measurement is described in Section 13.4.

In the surge-current test, suitable pulses are passed through the cell, with or without the peak working reverse voltage, as mentioned above. This test must not have a permanent effect on the reverse characteristic.

(5.4.7) Design Approval Tests.

When a rectifier has been designed and made experimentally in sufficient numbers to ensure consistency, it is essential to carry out certain proving tests so that reliability and operational life can be assessed.

Such tests include:

(*a*) Vibration and shock.
(*b*) Steady acceleration.
(*c*) Climatic cycling.
(*d*) Life under steady maximum conditions.
(*e*) Life under cyclic fluctuating loads.

Test (*e*) is particularly important for applications where the load is continually fluctuating. The cell must show freedom from thermal fatigue in the internal joints between the various components. The thermal path from the silicon to the cooling system is inevitably copper, so that expansion coefficients of $2\cdot5 \times 10^{-6}$ and 16×10^{-6} must be reconciled, if necessary by introducing intermediate layers of medium-expansion-coefficient materials. Soft-soldered joints can be particularly troublesome with such cyclic loads.

(5.5) Typical Types and Ratings

Table 1 gives a list of typical rectifier cells with ratings and other relevant data. Only the highest-reverse-voltage type of each class of rectifier is quoted; in general, there are several intermediate-voltage types in each class, other ratings being identical.

(6) OPERATIONAL CONSIDERATIONS

The efficiency of a silicon rectifier is very high. For instance, with a peak working voltage of 300 volts and a forward voltage drop of 1·1 volts, a cell efficiency greater than 99% is obtained. Although the internal loss is so small—less than 1% of the load power—it controls almost entirely the conditions under which the cell may be used.

In operation, most silicon rectifiers are mounted on a cooling system. To design a cooling fin the following information must be known:

(*a*) Maximum junction temperature, $\theta_{j\,max}$.
(*b*) Internal power loss of the cell at maximum junction temperature, P_i.
(*c*) Internal thermal resistance, junction to case, of the rectifier cell, S_i.
(*d*) Cooling air temperature, θ_{ca}.

If S_h is the thermal resistance of the cooling fin to the cooling air, we have

$$S_i + S_h = \frac{\theta_{j\,max} - \theta_{ca}}{P_i}$$

or

$$S_h = \frac{\theta_{j\,max} - \theta_{ca}}{P_i} - S_i \quad . \quad . \quad . \quad . \quad (6)$$

It will be helpful to review these various parameters in turn. The maximum junction temperature has already been discussed.

(6.1) Internal Power Loss P_i

The internal power loss consists essentially of the loss due to the forward current and forward voltage drop, the reverse loss rarely exceeding 5–10% of the forward loss at full-load current. Manufacturers normally quote data relating forward loss with load current, from which the power loss at maximum current can be obtained directly. For most practical purposes a good approximation to the forward loss is

$$P_f = 0\cdot95 V_f I_{mean} \quad . \quad . \quad . \quad . \quad . \quad (7)$$

Table 1

Characteristics and Ratings of a Typical Range of Silicon Rectifiers

Class	Type	Nominal current type	Crest working voltage	Peak transient voltage	Rated output current 25° C amb	25° C stud	100° C amb	100° C stud	Max. junction temp.	Thermal resistance Junction to ambient	Junction to stud	Construction
			volts	volts	amp	amp	amp	amp	deg C	deg C/watt	deg C/watt	
Miniature rectifier	MS5–H	0·25	300	300	0·25	—	—	—	150	600	—	All glass wire ended
Low-power rectifier	SJ601–B	0·5	600	600	0·7	—	0·15	—	120	100	—	Wire ended
	SJ601–A	1·0	600	600	—	1·6	—	0·5	120	—	30	Stud base
	SJ402–B	1·0	400	400	1·0	—	0·75	—	200	100	—	Wire ended
	SJ402–A	2	400	400	—	2·4	—	1·6	200	—	30	Stud base
Medium-power rectifier	SL401–A	10	400	400	—	15	—	8·5	150	—	4·5	Stud base
	SM410–A	30	400	400	—	40	—	20	150	—	2	Stud base
High-power rectifier	SN151–A	100	150	300	—	150	—	100	150	—	0·25	Plate base
	SP20D40	200	400	800	—	250	—	150	150	—	0·2	Plate base
Zener diodes	VR series B	—	3–12	—	0·5–0·15	—	0·35–0·10	—	250	100	—	Wire ended
	VR series A	—	3–12	—	—	1·3–0·4	—	0·7–0·2	200	—	30	Stud base
	VRX10	—	3–12	—	—	4–1	—	2–0·5	200	—	4·5	Stud base
Controlled rectifiers	CR5	5	300	300	—	7·5	—	1	120	—	2	Stud base
	CR10	10	300	300	—	15	—	2	120	—	2	Stud base
	CX100	100	200	200	—	150	—	50	120	—	0·20	Plate base

Since V_f, the peak forward voltage drop, is about 1·1 volts, this approximates to 1 watt per ampere of mean current.

(6.2) Internal Thermal Resistance S_i

The internal thermal resistance is defined as the temperature difference between the junction and the cell base, divided by the internal power loss. Values for typical cells are quoted in Table 1. At large values of temperature difference between the cell case and the cooling air, the thermal resistance decreases slightly owing to increased radiation from the cell surface, but the effect is small and can usually be ignored.

(6.3) Cooling Air Temperature θ_{ca}

The cooling air temperature must refer to the temperature of the air immediately before it comes into contact with the cooling fins, and may be higher than the ambient temperature outside the cubicle. For industrial use, 30° C is normal.

(6.4) Thermal Resistance of Typical Cooling Fins S_h

For a given set of operating conditions, the required thermal resistance of the cooling-fin system can now be calculated from eqn. (6). The thermal resistance depends upon fin design, and consideration of a few standard arrangements will indicate the order of values.

The simplest cooling fin consists of a flat square plate with the rectifier cell mounted at or near the centre. Efficient distribution of heat over the fins is obtained by using good thermal-conductivity materials such as copper or aluminium. Finned rectifiers are almost invariably used in bridge connection, the finned cells being mounted into stacks. A fin located near the centre of a stack has a restricted radiation, and this condition must be assumed when considering its thermal resistance. Fig. 7 shows a family of curves for square-plate fins, relating thermal resistance with fin area and thickness for natural and forced air cooling.

For high-power rectifiers, extruded aluminium fins are both

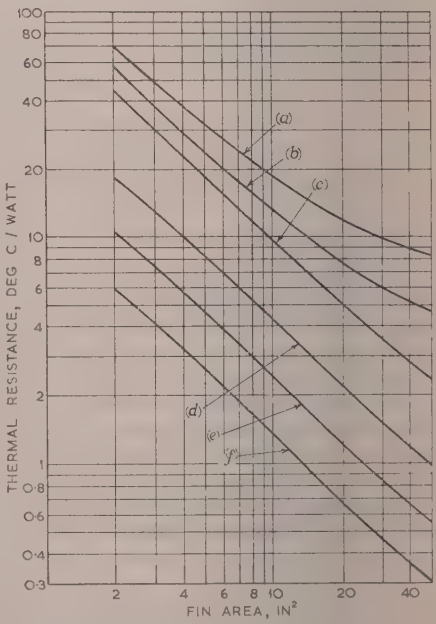


Fig. 7.—Thermal resistance of square cooling fins.

Convection-cooled (impeded radiation) blackened copper fins (Ambient temperature, 30°C; fin temperature, 100°C)
(a) 0·010 in thick.
(b) 0·030 in thick.
(c) 0·082 in thick.

Forced-air-cooled blackened copper fins 0·082in thick (Cooling air temperature, 30°C: fin temperature, 100°C)
(d) 500 ft/min.
(e) 1000 ft/min.
(f) 2000 ft/min.

Fig. 8.—Typical extruded aluminium fin.

convenient and economic. Fig. 8 shows a typical cross-section. A 12 in length of this fin mounted vertically has a thermal resistance of approximately 0·6° C per watt. With high-velocity cooling air available as in aircraft, a labyrinth type of heat exchanger (Fig. 11) gives very good results.

(6.5) Thermal-Equilibrium Conditions

In thermal equilibrium, the heat lost by the fin system equals the heat produced in the rectifier cell. For thermal stability, the rate of heat lost with temperature by the cooling fin must be greater than the rate of heat generation in the rectifier cell. This condition can best be illustrated graphically. In Fig. 9, curves (i) and (ii) show the variation of forward and reverse power loss with junction temperature for a typical silicon power rectifier, and curve (iii) shows the total loss. Curves (iv), (v) and (vi) show the thermal characteristics of three cell-plus-

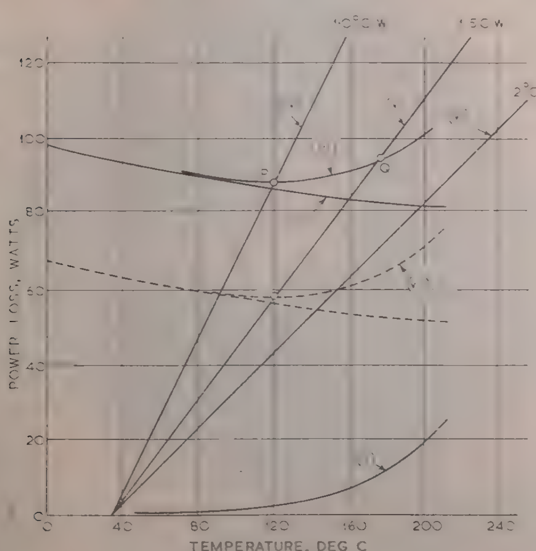


Fig. 9.—Thermal-stability considerations.

cooling-fin combinations, having overall thermal resistances, junction to ambient, of 1° C per watt, 1·5° C per watt and 2° C per watt, respectively. With fin (iv) and an ambient temperature of 35° C, equilibrium will be reached at a junction temperature of 120° C, as indicated by point P. If a smaller cooling fin with a higher thermal resistance as in curve (v) is used, the junction temperature will stabilize at the point Q, at approximately 170° C. To reduce the junction temperature to 120° C as with fin (iv), it will be necessary to reduce the forward current to about 70% of the original value, to give curve (vii). With fin (vi) and the original forward current, thermal-stability conditions are not met as the fin will not dissipate the heat generated in the cell.

(6.6) Parallel and Series Working

If certain precautions are observed, silicon rectifier cells may be operated in any series and parallel combination.

(6.6.1) Parallel Working.

There is seldom any necessity to operate low- or medium-current cells in parallel, since a higher-current-rated cell can be used. Thus, in general, only the highest-current cell is used in parallel connection. In the manufacture of such cells, very accurate control is exercised on the factors influencing the forward characteristic, and close limits, both upper and lower, are placed on the forward voltage during test, so that cells may be operated in parallel with little reduction in current rating.

(6.6.2) Series Working.

In moderate-voltage installations, when silicon rectifier cells are connected in series, the instantaneous voltage that appears across different cells may be non-uniform if

(*a*) The reverse voltage/current characteristics vary from cell to cell.
(*b*) There are variations from cell to cell in the minority-carrier storage time.

The latter will cause the cell with the shortest storage time, which first completes the clean-up of its carriers at the end of the conducting period, to withstand the whole string voltage for a short time. This is not very serious in itself, since the normal recovery period for the other cells is short compared with the rate of increase of voltage allowed by the circuit parameters, but the recovery of the first cell will delay the discharge of the others, as shown in an extreme case in Fig. 10.

Both causes of non-uniform sharing may be cured by connecting a resistor or capacitor across each cell, and this is frequently done, but the solution is not without disadvantages. If the cell voltage is equalized by such external means, the assistance which each cell derives from the others in series with it is lost, and it may therefore be necessary to limit the average voltage per cell to a lower value. The best course depends on the degree of control exercised in manufacture, and on other design factors, with the result that the practice of different manufacturers varies in this matter.

An important aspect of series operation is the possibility of increased reliability, since the reverse current through all the cells is limited to a value set by the best cell. If the reverse characteristic of a cell operating singly deteriorates, this leads to an increase in reverse current, and perhaps to thermal runaway. Such a cell operating in series with a stable cell will have its reverse current limited by the stable cell and thermal runaway will be prevented. Thus, in applications where extreme reliability is more important than economy of installation, it may be preferable to use, say, two 300-volt cells in series for a 600-volt output rather than one 600-volt cell.

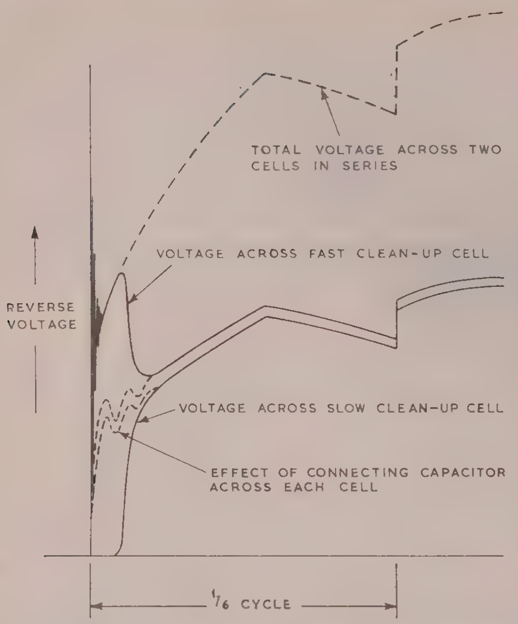


Fig. 10.—Series operation. Effect of hole storage.

(6.7) Surge Protection

The function of protective circuits in a rectifier installation is to safeguard not only the rectifier cells but also the other components in the equipment; in this paper protection will be discussed only from the point of view of the rectifier cell.

(6.7.1) Over-Voltage Surge Protection.

In view of the nature of the negative-resistance type of breakdown mentioned earlier it is imperative that no voltage surges exceed the peak transient reverse voltage rating of the cell. Voltage surges can be produced within the rectifier installation by transformer switching, hole-storage effects and the switching of inductive loads. Such surges can be limited in amplitude by means of a capacitor connected across either the input or the output of the rectifier bridge. For instance, to reduce the switch-off surge with Stalloy-cored transformers up to 2kVA, a suitable capacitance can be obtained from

$$C = \frac{340 \times (\text{volt-ampere rating})}{V_r^2} \mu\text{F} \quad . \quad . \quad . \quad (8)$$

where V_r is the crest working voltage across the rectifier cell.

The capacitance obtained from this relation is usually greater than that needed to deal with hole-storage effects.

It is advisable to carry out measurements on the supply voltage to check for voltage spikes, performing switching operations on adjacent equipment wherever possible. If such spikes are present and it is not possible to suppress them, a rectifier cell with a sufficiently high peak transient reverse voltage rating must be used.

(6.7.2) Over-Current Surge Protection.

The smallness of the silicon rectifier cell means that it has a low thermal capacity, and is limited in its capability to absorb over-current surges. Also, since it is so efficient, it can exert little limiting action on the current during an external fault such as a short-circuit on the output. Thus it is essential to include means for protecting the cell.

There are two main approaches: first, by arranging to interrupt the circuit before the current reaches the surge rating; and secondly, to limit the current amplitude to the surge rating of the cell by including suitable impedance in the circuit.

Low-power cells are generally protected in the first way, by means of fuses on the a.c. side, and high-power cells by a combination of circuit-breakers, fuses and series reactance.

This subject has been discussed at length by Gutzwiller.[5]

(7) APPLICATIONS

(7.1) General

It would appear that the silicon rectifier is eminently suitable for any rectifier installation in ambient temperatures up to the order of 150° C. By suitable choice of operating junction temperature it should be possible to compete with any other known system of rectification. However, at present this may not be economic or sensible in many cases, and so the use of the silicon rectifier will steadily build up in those applications where it offers most advantage.

(7.2) Supplies for High-Power Electrolysis

The germanium rectifier soon found widespread application in high-current electrolysis supplies, and the silicon rectifier will undoubtedly also be used in such installations, particularly when the direct output voltage exceeds 250 volts.

(7.3) Traction Vehicles

The compact nature of the silicon rectifier coupled with its low weight makes it very suitable for use on railway vehicles. This field of application was also opened up successfully by the germanium rectifier, and it is evident that the silicon rectifier will be widely used. A typical power requirement for a motor-coach supply is 750 kW at 1 kV, and for a locomotive, 2 500 kW at the same voltage.

(7.4) Electronic Equipment

Silicon rectifiers are very suitable for use in low- and medium-power supply units for electronic apparatus, and are already being used in d.c. supplies, magnetic amplifiers, power control circuits and a wide variety of special circuits. As a result of the low forward voltage drop, d.c. supplies can be designed with a much better regulation characteristic.

(7.5) Aircraft Applications

It is in aircraft applications that the advantages of the silicon rectifier are most beneficial. The small size, low weight and high efficiency have all proved to be most attractive for use in the ever-increasing amount of electrical and electronic equipment in the modern aircraft. Equipment in aircraft under development at present may incorporate silicon rectifiers of various types, amounting to several hundreds per aircraft. Fig. 11 shows a 28-volt 500 amp rectifier for general aircraft supplies.

(7.6) Brushless Alternators

An important application for silicon rectifiers is in the brushless alternator. In this type of machine, slip rings and commutators have been completely eliminated. The output from a rotating-armature a.c. exciter is rectified by silicon rectifiers mounted on the rotating shaft and fed along the shaft to the main alternator rotor.

Two developments can be quoted. The first is a 40 kVA alternator for aircraft supplies. The rectifier assembly, consisting of six 10 amp silicon rectifier cells mounted on an end-plate, is shown in Fig. 12(*a*). The rectifiers are rotated at 6 000 r.p.m. During the development of this alternator, rectifiers were rotated at speeds resulting in accelerations of 2 500g with satis-

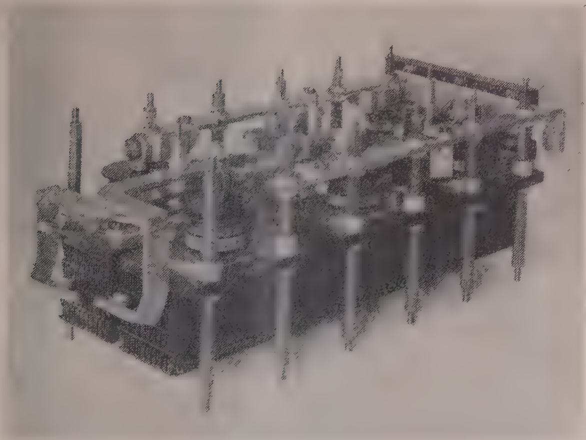
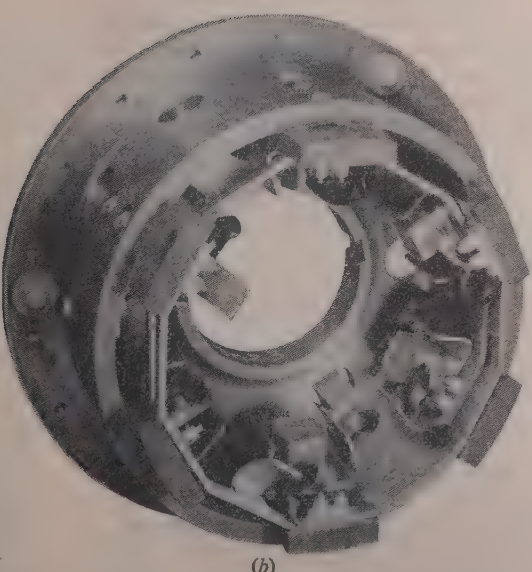


Fig. 11.—28-volt 500 amp rectifier assembly for aircraft applications.

(a)



(b)

Fig. 12.—Silicon rectifier assemblies for brushless alternators.
(a) For a 40 kVA alternator.
(b) For a 1000 kVA alternator.

factory results. A large 1 000 kVA machine for marine use has also been developed, the rectifier assembly being shown in Fig. 12(b). It uses six 150 amp cells, type SP.15D, rotating at 1 800 r.p.m.

(8) SPECIAL TYPES

(8.1) Voltage-Reference (Zener) Diode

The voltage-reference (or Zener) diode is a silicon rectifier using very-low-resistivity silicon, such that peak reverse voltages as low as 2 or 3 volts are obtained. It can be used as a voltage reference or voltage limiter in much the same way as a gas discharge tube. Characteristics of this device have been discussed in a recent paper by Garside and Harvey.[6]

Two features are of particular interest. The slope resistance passes through a minimum value for diodes having a breakdown voltage around 7 volts, and the temperature coefficient of the breakdown voltage approximates to zero for diodes of $5 \cdot 5$ volts, being negative for lower- and positive for higher-voltage units. A typical low-power 6-volt diode has a slope resistance of about $1 \cdot 8$ ohms at 20 mA and a temperature coefficient of breakdown voltage about $+2 \cdot 0$ mV per deg C. Such units are finding considerable application as precision voltage references. With suitable circuits, including compensation for the residual temperature coefficient, stabilities an order of magnitude better than that possible with a standard cell reference can be obtained.

(8.2) E.H.V. Assemblies

Series operation of silicon rectifiers has been mentioned earlier in the paper. If a high transient voltage is impressed across a string of rectifier cells, the proportion of the transient appearing across the cells at the h.v. end of the string will be very high because of the voltage-divider effect of the cell-junction capacitance and the capacitance of the cell to earth. Typical values of the above capacitances may be 8 and 1 pF, respectively. If a surge of 10 kV occurs on the line, then, depending upon the actual number of cells in the string, up to 3 kV may appear across the end cell and cause breakdown. A succession of such surges will eventually destroy the whole string.

A simple remedy is to connect across each cell a capacitor having a value high compared with the cell-to-earth capacitance. In practice, it is convenient to make the capacitance large enough to assist sharing at mains frequency, and values up to $0 \cdot 005\,\mu F$ are used. This capacitor also takes care of the hole-storage effect mentioned earlier.

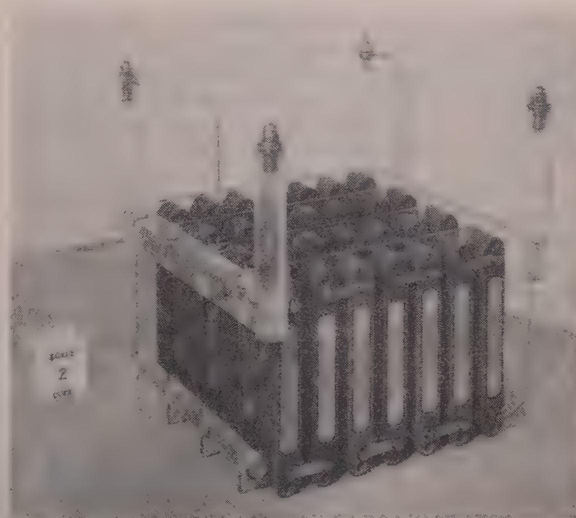


Fig. 13.—70 kV 1 amp silicon rectifier assembly.

A typical assembly for a peak transient reverse voltage of 10 kV consists of a suitable number of 300 or 400-volt cells in series, together with parallel capacitors. It is moulded in resin with overall dimensions of 1 in × 1 in × 7 in. Fig. 13 shows a full-wave bridge assembly for 70 kV peak reverse voltage, using seven 10 kV units in series in each arm of the bridge. Overall dimensions of this assembly are 12 in × 12 in × 8 in. In use it is oil immersed, and can carry load currents up to 1 amp. Such assemblies are being developed for use in dust-precipitation equipment.

(8.3) Miniature Rectifier

The miniature rectifier is mentioned as a special type merely to emphasize its very small dimensions. It is a wire-ended glass-encapsulated unit of ⅛ in diameter and ¼ in long, and is available for crest working reverse voltages of up to 600 volts and a current rating of 400 mA. The type is obviously very convenient for a lower-current version of the e.h.t. assembly described above.

(9) SILICON CONTROLLED RECTIFIERS

(9.1) General

The silicon controlled rectifier is a solid-state device which has characteristics similar to those of the gas thyratron or the grid-controlled mercury-arc rectifier, in that, while normally in a blocking or non-conducting state, it can be triggered into conduction by means of a small electrical pulse. Its reverse characteristic is similar to that of a conventional silicon rectifier. In the forward direction it has two modes of stable operation. In the first mode the impedance is high, the characteristic being similar to the reverse characteristic. In the second mode it has a low impedance, and the characteristic is similar to the forward characteristic of a conventional silicon rectifier. Once in this conducting mode, it will remain so until the main current is reduced almost to zero, when it returns to the non-conducting mode. The device has several advantages that make it very attractive:

(*a*) Very low voltage drop in the conducting mode of between 1 and 2 volts.
(*b*) Fast switch-on time of a few microseconds.
(*c*) Fast recovery time, at least an order of magnitude faster than that of the thyratron.
(*d*) Absence of a filament, and thus no heating-up time or excess heat generation.
(*e*) Very small size and robust construction.

(9.2) Operation

The controlled rectifier consists basically of a piece of silicon with a *p–n–p–n* structure as shown diagrammatically in Fig. 14. A simplified version of its operation can be obtained by considering it as a *p–n–p* transistor and an *n–p–n* transistor coupled together with a common collector junction J_2. With the polarity shown, there are three components to the current passing through the junction J_2, first, a 'leakage' current I_{c0} which is the saturation current due to thermal generation in n_1 and p_2; secondly, a hole current $\alpha_1 I$, α_1 being the effective current gain of the p_1–n_1–p_2 transistor; and thirdly, an electron current $\alpha_2 I$, α_2 being the effective current gain of the n_2–p_2–n_1 transistor. Thus the total current passing through the junction J_2 will be $(I_{c0} + \alpha_1 I + \alpha_2 I)$. This must be equal to the main current I, and so

$$I = I_{c0} + \alpha_1 I + \alpha_2 I \quad . \quad . \quad . \quad . \quad . \quad (9)$$

Therefore

$$I = \frac{I_{c0}}{1 - (\alpha_1 + \alpha_2)} \quad . \quad . \quad . \quad . \quad . \quad (10)$$

If the sum of α_1 and α_2 is less than unity, the main current will be of the order of I_{c0} and the device will have a high impedance, the junction J_2 holding off the applied voltage. However, if the sum of α_1 and α_2 is equal to unity, the denominator of eqn. (10) will be zero and the main current will be large, being limited only by the external circuit resistance. Thus, if the sum $(\alpha_1 + \alpha_2)$ is suddenly increased to unity, the device will switch from the high-impedance to the conducting mode.

The current gain of a silicon transistor at low emitter currents is small, and increases as the emitter current is increased. Thus, if the emitter current of transistor n_2–p_2–n_1 is increased by injecting a current at A, the values of α_2 will be increased. This will increase the main current I, which will in turn increase α_1. When the sum $(\alpha_1 + \alpha_2)$ reaches unity the device will switch. Thus the device may be triggered into the conducting mode by injecting a small current pulse into electrode A.

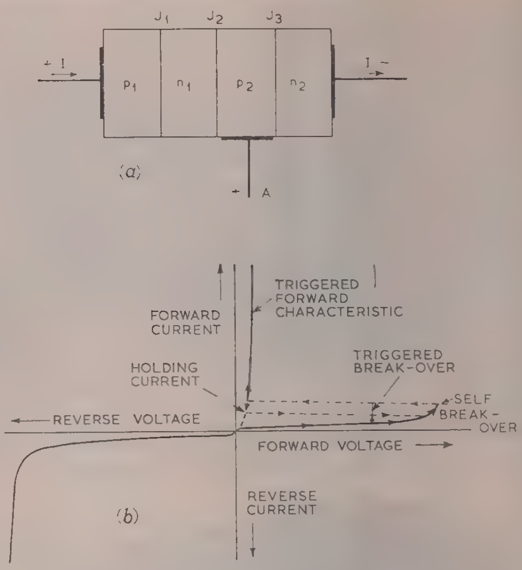


Fig. 14.—Silicon controlled rectifier.
(*a*) Diagrammatic representation.
(*b*) Characteristic curves.

(9.3) Construction and Characteristics

There are several ways of manufacturing the controlled rectifier —by diffusion, by differential segregation, or by a combination of either with alloying. A convenient method which has formed the basis of several early designs is, first, to produce a *p–n–p* structure by diffusing an acceptor into both sides of an *n*-type wafer, and then to alloy a donor to one of the *p*-type surfaces. The trigger connection is made to this *p*-type surface by alloying an acceptor to form an ohmic contact. The assembly is shown diagrammatically in Fig. 15.

It is now possible to produce units suitable for operation at voltages up to about 400 volts. A unit with a current rating

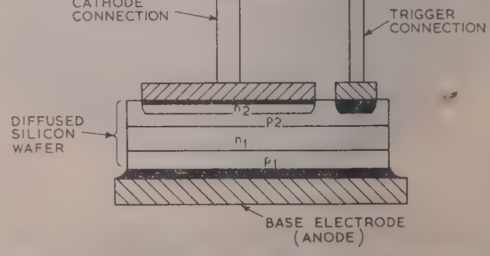


Fig. 15.—Diagrammatic section of controlled rectifier.

of 10 amp has been developed, and it has a forward voltage drop at this current of about 1·5 volts. Typical trigger requirement is in the range of 10–50 mA at 2 volts. The voltage rating is based upon the maximum reverse voltage or the forward self-break-over voltage, whichever is the lower. These voltages are determined at maximum junction temperature, which is limited to 120° C. The forward current rating depends upon the forward power loss in the conducting mode, the thermal resistance of the assembly and the ambient temperature. Cells suitable for higher currents, up to about 100 amp, have been demonstrated.

Fig. 16 shows a 10 amp cell and an experimental 100 amp one.

(*a*)

(*b*)

Fig. 16.—Silicon controlled rectifier cells.

(*a*) 10 amp cell type CX10.
(*b*) Experimental high-current cell.

(9.4) Applications of the Controlled Rectifier

Since the operational characteristics of the silicon controlled rectifier are similar to those of the gas thyratron it can be used in similar applications. Moreover, with its faster switch-on and recovery times, together with its low voltage drop and absence of a heated cathode, it can be used for new applications for which thyratrons are not suitable.

The one aspect in which the controlled rectifier is not yet comparable with the thyratron is in the maximum voltage rating. However, it is possible to use controlled rectifiers in series if they are shunted with voltage equalizing resistors. Parallel operation of cells is also possible if due attention is paid to the matching of their forward conduction characteristics.

Fig. 17 shows several basic circuits. Fig. 17(*a*) is probably the simplest application—a d.c. static switch. To switch on, the trigger circuit is momentarily energized by pressing the 'on' button. The controlled rectifier conducts and the capacitor C then charges up to the supply voltage through R. When the 'off' button is pressed, the capacitor is connected across the controlled rectifier, impressing a negative voltage for the few microseconds necessary to return the cell to the blocking condition. Fig. 17(*b*) shows a full-wave a.c. switch. When the control contacts are closed, current is fed through the rectifier D1 and the resistor R to the trigger electrode of the controlled rectifier CR1 and so triggers it into the conducting state. At the end of the half-cycle CR1 returns to the blocking condition, and at the start of the second half-cycle CR2 is triggered via the rectifier D2 and resistor R. In each case R limits the trigger current, and the rectifiers D1 and D2 prevent reverse current flowing in the trigger circuits.

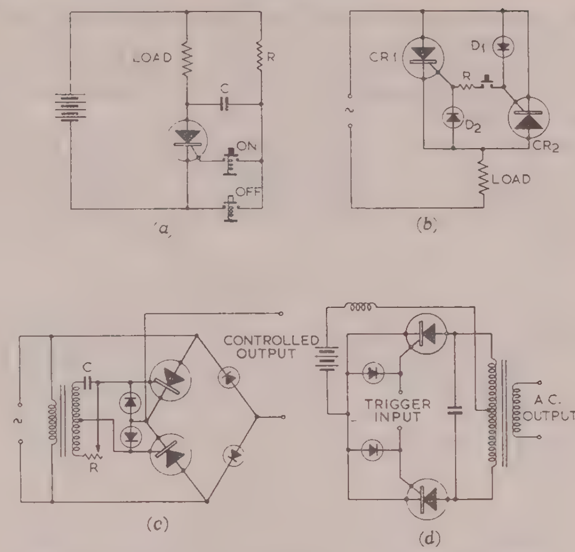


Fig. 17.—Basic circuit applications of controlled rectifiers.

(*a*) D.C. switch.
(*b*) A.C. switch.
(*c*) Full-wave phase control.
(*d*) D.C./A.C. inverter.

In each of these switching applications, the advantages of the controlled rectifier are readily apparent. The use of a high-current contact with the attendant contact wear and bounce is avoided and the possibility of remote control is introduced.

Control of the d.c. output of a rectifier circuit can be effected by varying the phase difference between triggering current and the applied voltage. Fig. 17(*c*) shows a full-wave phase-controlled circuit, the phase shift being obtained by means of an *RC* network.

Fig. 17(*d*) illustrates the use of the controlled rectifier in d.c./a.c. inverter circuits.

A paper[7] dealing with industrial applications of the controlled rectifier was read before The Institution during the International Convention on Transistors and Associated Semiconductor Devices in 1959. As higher-current versions of the device become available, it is certain that they will be applied in increasing numbers to high-power industrial duties, with controlled outputs in the megawatt range.

(10) FUTURE TRENDS AND CONCLUSIONS

The characteristics and versatility of the silicon rectifier appear to be so desirable that it would appear reasonable to forecast that, just as the selenium rectifier was the standard metal rectifier for a period of some 20 years, the silicon rectifier will be a standard type for many years in the future. It may be that, for very special industrial and aircraft applications, operation at ambient temperatures higher than those possible with silicon will be required, and, in this connection, work is already proceeding on high energy gap compounds such as silicon carbide, gallium arsenide and aluminium antimonide, with a view to developing rectifiers for use in the range 250–500° C. Low-voltage silicon-carbide *p–n* junctions have already been demonstrated operating at 500° C. However, unless there is a major break-through in the technology of these compounds, it does not appear likely

that they will be able to challenge silicon for general use at moderate temperatures.

The silicon controlled rectifier is of outstanding importance and its use will undoubtedly be extended to the very-high-power field.

Thus it can confidently be predicted that the silicon rectifier in its various forms is here to stay, and that design engineers can look forward to a comprehensive range of cells that will remain stable in design and characteristics for a long time to come.

(11) ACKNOWLEDGMENTS

The authors wish to state that the paper has only been made possible by the co-operation of many of their colleagues in the A.E.I. (Rugby) Research Laboratory, the A.E.I. Heavy Plant Division and the A.E.I. Electronic Apparatus Division. They also wish to thank the management of the A.E.I. (Rugby) for approval to publish the paper.

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(13) APPENDICES

(13.1) Theoretical Aspects of the Avalanche Voltage and the Correlation between the Avalanche Voltage and the Properties of the Silicon Material

When a reverse voltage is applied to a *p–n* junction, the holes of the *p*-type region and the electrons of the *n*-type region are driven apart and carrier depletion occurs near the junction. A current through this depleted region is only possible by the movement of minority carriers (electrons from the *p*-type, holes from the *n*-type), which are, however, not available in high concentrations. Therefore, the current depends on the rate of the thermal generation of minority carriers.

The specific thermal generation rate of minority carriers reaches a constant value after the concentration of the majority carriers has dropped below a certain level.[8] The width of the depleted region increases only slightly with the reverse voltage V, and as a consequence of this, one has a so-called 'saturation current' (cf. Section 13.2), which is only slightly voltage dependent.

However, at higher reverse voltages the velocity of the carriers which are accelerated by the field can become so high that new carriers are generated by impact ionization, and the current increases considerably.[9] This can cause a chain reaction and then the current increases abruptly, and we have an 'avalanche breakdown'.

Almost all the electric field lines in the rectifier start and end at the space charges provided by the ionized impurities in the region which is depleted of majority carriers. The impact ionization and the onset of a chain reaction depend on the field strength and on the width of the region in which the field strength is high enough for impact ionization to occur; it can be shown theoretically that the avalanche voltage increases with a decrease of the impurity concentration in the base region. The resistivity of the silicon from which the rectifier is made is a measure of this impurity concentration (provided that no change is produced during manufacture), and therefore we can relate the avalanche voltage V_B to the resistivity of the base material. For resistivities of the base layer higher than 2 ohm-cm the following approximate relations have been measured by Shields:[10]

$$V_B \simeq 23\rho_p^{0\cdot75} \text{ for a } p\text{-type base} \quad . \quad . \quad . \quad (11)$$

$$V_B \simeq 48\rho_n^{0\cdot75} \text{ for an } n\text{-type base} \quad . \quad . \quad . \quad (12)$$

where V_B is measured in volts and ρ in ohm-cm.

If the voltage is so high that the region which is depleted of majority carriers extends to the highly doped base-contact region (punch-through), then, at even higher voltages the field line density depends only slightly on the impurity concentration in the base and the avalanche voltage effectively depends only on the width w of the base layer and not on its resistivity. As a rough approximation,

$$V_B = 1\cdot2 \times 10^5 w \quad . \quad . \quad . \quad . \quad . \quad (13)$$

where V_B is measured in volts and w in centimetres.

In general, the avalanche voltage is the smaller of the two values of V_B evaluated from eqn. (11) or (12) and eqn. (13).

(13.2) Theoretical Aspects of Reverse Saturation Current

As has been shown in Section 13.1, the reverse current is determined by the effective rate of carrier generation, which consists of two parts—rate due to thermal generation, G_{th}, and a rate due to impact ionization, G_i. We shall here discuss G_{th}, which leads to the so-called 'saturation current'. This saturation current is

$$i_s = AeG_{th} = Ae\int_0^l g_{th}dx \quad . \quad . \quad . \quad . \quad (14)$$

From Reference 8, in regions where either the majority or minority carrier density has dropped by an order,

$$g_{th} = \sum_{j=1}^{m} \frac{\bar{n}_i^2 n_{tj}}{\left(\frac{\bar{n}_h + k_{hj}}{\alpha e_j}\right) + \left(\frac{\bar{n}_e + k_{ej}}{\alpha h_j}\right)} \quad . \quad . \quad (15)$$

where $$\bar{n}_i^2 = N_c N_v \exp\left[-\frac{(W_c - W_v)}{kT}\right] \quad . \quad . \quad . \quad (15a)$$

$$k_{hj} = N_v \exp\left[\frac{-(W_{tj} - W_v)}{kT}\right] \quad . \quad . \quad . \quad . \quad (15b)$$

$$k_{ej} = N_c \exp\left[-\frac{(W_c - W_{tj})}{kT}\right] \quad . \quad . \quad . \quad (15c)$$

and where different types of recombination centres $T_1, T_2 \ldots T_m$ with energy levels $W_{T1}, W_{T2} \ldots W_{Tm}$, hole recombination constants $\alpha_{h1}, \alpha_{h2} \ldots \alpha_{hm}$ and electron recombination constants $\alpha_{e1}, \alpha_{e2} \ldots \alpha_{em}$ are assumed to be present in concentrations $n_{t1}, n_{t2} \ldots n_{tm}$, respectively. W_c is the lowest energy level in the conduction band, and W_v is the highest level in the valence band. Usually, the base width will be small and have a high resistivity to give the best combination of high forward current and high avalanche voltage; in this case, the region with a low majority carrier concentration, which has usually a higher specific effective generation rate and is slightly voltage dependent (before punch-through is reached), will be predominant, and the 'saturation current' at the higher voltages is approximately

$$i_s = Ael \sum_{j=1}^{m} \frac{\bar{n}_i^2 n_{tj}}{(k_{hj}/\alpha_{ej}) + (k_{ej}/\alpha_{hj})} \quad . \quad . \quad . \quad (16)$$

where l is the width of the region which is depleted of majority carriers. It can be seen from eqn. (16) that the most effective types of centre are those having an energy level near the centre of the energy gap, and that, for a low saturation current, it is important to keep the concentration of these very low. From eqn. (16) it is also of advantage to use a semiconductor with a high energy gap $(W_c - W_v)$; for the same saturation current, the concentration of the recombination centres can be much higher in a semiconductor with high energy gap than in one with low energy gap. As a consequence it has been possible to reduce the saturation current in a silicon rectifier to a value lower than in a germanium rectifier, if both are operated at the same temperature. Alternatively, the temperature of a silicon rectifier can be increased considerably over that of a germanium rectifier before the saturation currents become comparable.

The concentration and type of recombination centres in bulk silicon are related to the lifetime of the carriers in it.[12] In the general case, one may assume that, of two rectifiers having the same base resistivity but different lifetimes, the one with longer lifetime has a lower saturation current.

When manufacturing rectifiers, one has to consider two competing effects. For the same voltage, the region which is depleted of majority carriers, and in which the specific effective thermal generation rate is high, increases with resistivity, whereas the impact ionization rate decreases with resistivity. In consequence, one would expect that the best blocking properties are reached in rectifiers made from material having a certain optimum resistivity.

(13.3) Forward Characteristics of Power Rectifiers

The power rectifiers considered in the paper can be discussed in terms of a $p + sn+$ model in which the s-region refers to the slightly doped n-type or p-type original semiconducting material; in addition, the important range in the forward direction of current flow is that of appreciable current density, up to several hundred amperes per square centimetre. Under these conditions of current flow the carrier densities in the central s-region are, in general, higher than the equilibrium concentration of majority carriers in this region. Thus conditions of high-level injection prevail in the semiconductor.

For high-level injection[13] the forward characteristic of the rectifier is independent of the resistivity of the s-region and is determined mainly by

(a) Thickness of the s-region.
(b) Diffusion length of carriers in this region and its variation with injected carrier density.
(c) Doping levels in the recrystallized $p+$ and $n+$ regions of the contacts.

In general, the smaller the thickness of the s-region the greater is the forward current at a particular voltage. However, a limit is placed on the minimum thickness of this region by the effects of space-charge penetration on the reverse voltage [see eqn. (13)]. Thus the minimum thickness of the s-region compatible with reverse voltages in excess of 1 kV is approximately 0·01 cm. For cases of practical interest in power rectifiers, the greater the diffusion length of carriers the greater is the current at a particular voltage. In the practical rectifier the doping levels in the $p+$ and $n+$ regions are generally high (10^{18} to 10^{20} impurities per cubic centimetre), and it would appear that only at current levels greater than a few hundred amperes per square centimetre will the resistivity of these regions become important.

Analytically, the general form of the forward characteristic has been given by Shields,[13] who finds

$$I = A\frac{2en_i D_s}{L_s} \tanh \frac{d}{L_s} \exp \frac{e}{2kT}(V_{app} - V_s) \quad . \quad (17)$$

$$V_s = \frac{2kT}{e} \sinh \frac{d}{L_s}\left[2 \arctan\left(\exp\frac{d}{L_s}\right) - \frac{\pi}{2}\right]$$

which is a function of temperature and the ratio d/L_s.

When the diffusion length is independent of injected carrier density, the current will be exponentially related to the applied voltage with a dependence of the form $\exp \frac{e}{2kt} V_{app}$; V_s is, in this case, a constant. If the diffusion length is dependent on carrier density, the simple exponential relationship will no longer be observed since V_s will vary appreciably with current. In general, the diffusion length decreases with injected carrier density and results in an increase of V_s with injected carrier density.

Empirically, it has been possible[14] to derive the variation of diffusion length with injected carrier density from investigations of the shape of the forward characteristic.

Analysis of eqn. (17) shows that the slope of the forward characteristic, $d(\log_e I)/dV$, decreases as the current density is increased. For the case of d/L_s less than unity, at low injected densities, the slope is initially greater than $e/2kT$ and decreases below this value with increasing current density.

Empirically, it also appears possible to take into account the variation of diffusion length with injected carrier density by expressing eqn. (17) in the form

$$V = IR + \frac{nkT}{e} \log_n \frac{I}{I_0} \quad . \quad . \quad . \quad . \quad (18)$$

where I_0, n and R are constants, and R and I_0 are temperature dependent and increase with increasing temperature.

This latter equation is in a convenient form for calculation of circuit behaviour under normal and overload current conditions;

it matches the average rectifier characteristic to within a few per cent from less than 10 to more than 1 000 amp/cm. It is the basis of the reference to 'resistive' and 'exponential' components of the forward characteristic in the text of the paper (Section 5.2).

(13.4) Method of Measuring the Thermal Resistance of Silicon Rectifier Cells

The measurement of the internal thermal resistance of a rectifier cell involves the measurement of junction temperature, stud temperature and the internal power loss. The thermal resistance is then the difference between the junction and stud temperatures divided by the power loss. The stud temperature can be measured with a thermocouple, and a method of measuring the junction temperature for a measured internal power loss is described. Fig. 18 shows the circuit arrangement for the measurement.

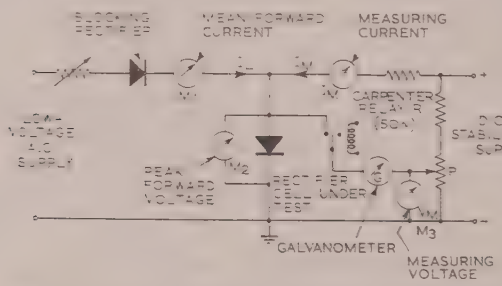

Fig. 18.—Circuit for thermal-resistance measurement.

The junction temperature is measured by using the fact that the forward voltage drop of a silicon *p–n* junction decreases with increase of temperature, by about 1·8 mV per deg C. Referring to Fig. 18, a half-wave rectified forward current I_L is fed to the cell under test. From the readings of mean forward current on M_1 and peak forward voltage on M_2, the internal power loss can be determined. A small forward direct current I_m, about 1% of the rectified current, is also fed into the cell from a constant-current source. A Carpenter relay R is operated from the 50 c/s supply and phased so that, during the second half-cycles, the voltage V_m, developed across the cell by the constant current I_m, can be determined. This voltage is calibrated in terms of junction temperature. The blocking rectifier D must rectify the full-load current and have a reverse leakage current small compared with I_m.

Measurement procedure is as follows. With no load current, the cell under test is heated in an oven and a calibration of measuring voltage V_m against temperature is obtained. To measure V_m, the potentiometer P is adjusted to give zero balance on the galvanometer G and the reading noted on M_3. With the cell in the required ambient conditions, V_m is noted, to give the initial junction temperature, and then the load current I_L is switched on. The cell will heat up, and the potentiometer P is again adjusted for balance to obtain the new value of V_m and thus the new junction temperature. Knowing the junction temperature rise and the internal power loss the thermal resistance can be calculated.

Since the relay contacts pass negligible current, the method can be applied to any size of rectifier cell if a suitable blocking rectifier is available.

The method can be adapted for production testing if a typical temperature dependence of V_m is assumed. With no load current, the potentiometer P is adjusted for balance and then set to a lower value corresponding to the maximum allowable junction temperature. The full-load current is switched on and the reading on the galvanometer G must not increase beyond the balance point, indicating that the junction temperature is less than the maximum allowable value.

DISCUSSION BEFORE THE INSTITUTION, 2ND FEBRUARY, 1961

Major L. H. Peter: The late Professor Fortescue, when opening the discussion on the first of several papers presented to The Institution relating to crystalline rectifiers, stated that it was extraordinary how many devices in the field of electrical engineering were used before their theory was really known. They became useful articles of commerce, but their full potential was never realized until all the principles underlying their action were understood. The silicon rectifier seems to have reached the stage when a great deal is understood of the action involved, and we are very much indebted to people like Shockley for explaining the action of these devices. It has taken 40 years to reach this point.

The authors seem to feel that the silicon rectifier represents finality, except for the special high-temperature cases. But who is to say that we shall not be able to produce crystals from other materials and substances, even some of the elements, having still better gap figures than silicon? We are now at a stage when we want help in other directions. We have a most powerful device for electrical engineers, which has been almost entirely produced by technological physicists. There is practically no machinery, electrical windings or anything like that, but we have high temperatures, gaseous states, and very intricate and clever methods of drawing crystals and refining. The authors state that it is necessary to think of impurities in terms of one part in 10^9. I doubt whether anything else in the field of electrical engineering operates at that scale of purity. It is difficult even to provide a container which can be used without introducing impurities on that scale. We may have to look in the future for help to chemists, and perhaps metallurgists, who may be able to give us containers which can be used without introducing impurities.

At present we introduce impurities and then zone-refine them, by the melting-zone process, out of the crystal at the time of producing a mono-crystal, but it should be possible in the future to shorten that long and very intricate process. Many of the physicists concerned have spent long hours in producing one crystal, and even then they cannot say that it is perfect. We still occasionally have to consider the last step, namely the degree of perfection that we can get in a crystal.

Having reached the stage where these devices are practical propositions, we must reconsider the nomenclature. There is no set method of defining a cell's possibilities, and it takes many formulae or expressions to give all the information required. No doubt as standardization proceeds we shall be able to give tabular lists of types in such a way that all their characteristics can be understood, but we have not reached that stage yet.

There is some mention in the paper of the protection of these devices against surges. One method not given is the use of one rectifier as a surge suppressor for another. The selenium rectifier, which has a reverse resistance characteristic which drops off very rapidly (something like the 11th power law), is often used to protect germanium or silicon rectifiers which might otherwise be destroyed. This is a comparatively cheap and not always too bulky a method. Many more protection devices will be developed to look after both silicon and germanium rectifiers.

Progress is going on at a very rapid rate in most of the countries interested in electrical engineering, and I am very glad that the International Electrotechnical Commission is about to reach the stage of publishing recommendations to deal with this. I hope that a national specification will follow those recommendations.

Mr. J. E. Boul: It is stated in Section 5.4.2 that, for the smaller rectifiers, it is desirable to have only one reverse-voltage rating, the crest working and maximum recurrent voltages being equal to the peak transient reverse voltage, but for large rectifiers it is decided to have two values, a higher one for the peak transient voltage than for the crest working voltage. This suggests that there is a time limitation applying to the peak transient reverse voltage in the case of the higher-power cell. If so, I shall be interested to know what time limitation is imposed.

In Section 6.1, I am very surprised that 'the reverse loss rarely exceeds 5–10% of the forward loss at full-load current'. I should like to have the authors' views on whether this value is typical of the larger-current power cell. It seems almost an order of magnitude higher than it should be.

In Fig. 6 we have a ratio of a little over 3 : 1 between the 1-cycle and 60-cycle rating for the 10 A cell. It would be interesting to know whether the same sort of characteristic is sustained for the larger-current cells and whether even larger-current cells are feasible, while still retaining such a ratio. This question is of considerable significance, in that the cell must carry the short-circuit and fault currents imposed by the duty to which the cell is applied. It is quite common to find that it is not possible to use the cell at its maximum continuous rating but that the number of parallel cells which have to be put in a given rectifier assembly is determined by the short-circuit conditions.

Do the maximum junction temperatures given in Table 1 represent a maximum in the same sense that the peak transient voltage is given as an absolute maximum, or have these maximum junction temperatures something in hand to enable the cell to withstand the effects of short-time overloads or faults?

Mr. A. Langridge: Crystal material may be specified in terms of resistivity, lifetime and dislocation density. It is well known that materials having identical specifications give very dissimilar results when used in devices. On close examination of the material used in fabricating these devices it is observed that certain imperfections are associated with the crystal material which may be brought out by using special techniques. When imperfections are present in a slice of silicon material and this is fabricated into a device there are four types of imperfection which affect characteristics, and unless these are noted during manufacture the effect may easily be overlooked. The imperfections can be tabulated and are generally classified as lineage, slip, high density of dislocations in preferred regions, and high dislocation density. Fig. A illustrates the gross defects appearing in some materials, and by avoiding high etch-pit density, slip and lineage, devices having sharp characteristics as shown in Fig. B are possible.

Fig. A.—High etch-pit density, bad slip and lineage.

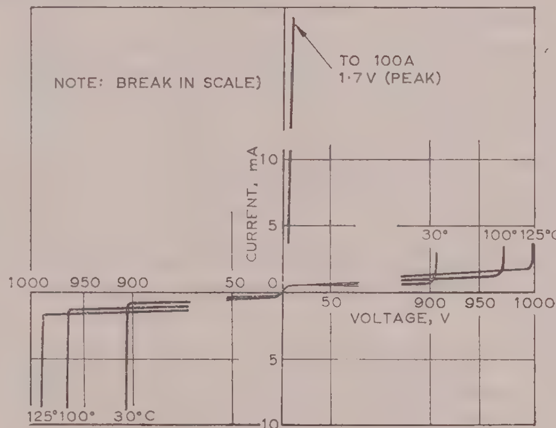

Fig. B.—Trinistor characteristics.
Typical variation with temperature.

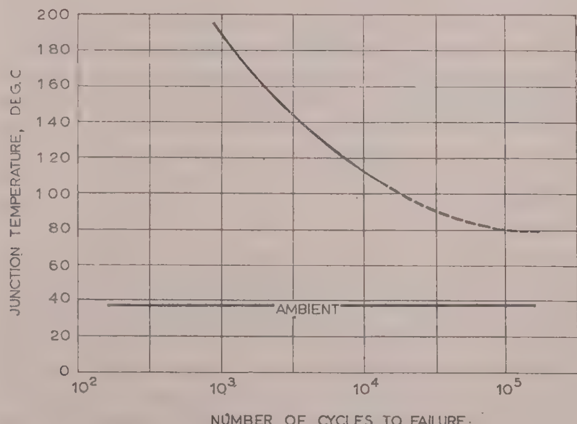

Fig. C

I will now deal with experiments which have been carried out in exploring the phenomenon of thermal fatigue. Fig. C illustrates the relationship which exists with silicon cells having a $\frac{5}{8}$ in-diameter junction soft-soldered to the base. The number of cycles required to failure varies in accordance with the junction temperature and diameter. Various experiments have been carried out using different materials interposed between the silicon and the copper base. Although the experiments indicate

that slight improvements may be made, the only successful solution has been the hard soldering of all the components making up the diode. When junctions are mounted in this manner the stress applied to the rectifier during the cyclic temperature excursions occurs within the elastic region of each component and the cell has an infinite life.

In view of these data I should like to know how the authors are able to reconcile the silicon material, which has a thermal expansion coefficient of the order of 5×10^{-6} per deg C, with copper, for which the figure is 17×10^{-6} per deg C. I should also like to have views on the use of silicon material which has imperfections of the kind which I have described.

Mr. D. R. Coleman: As a member of the staff of another major manufacturer (and user) of silicon power rectifiers I substantially support the paper. I regard the device processing data, however, as a statement of one manufacturer's experience; for example, I question the authors' views on lifetime degradation and base thickness.

By describing a particular range, the paper suggests that silicon rectifiers at present marketed are limited to 600 V rated crest working voltage. Higher rated devices are, in fact, now available. Similarly, rated peak transient reverse voltages greater than 1·2 kV are now possible.

Table 1 gives different crest working voltage and peak transient voltage ratings for only the higher-power rectifiers. If the rated peak transient voltage is below turn-over (Section 5.4.3), methods of test and ascribing ratings are presumably the same for all sizes of rectifier. Why should we differentiate between smaller and larger types for crest working and peak transient voltage ratings?

The three definitions in Section 5.4.2 of reverse voltage rating are most welcome and will greatly assist the application of silicon rectifiers. Should the peak transient voltage rating be time- or energy-dependent, or perhaps both?

Two statements deserve comment. First, reverse voltages during surge-current testing are considered necessary only with the larger cells (Section 5.4.5), since these are normally used in parallel and separately fused. But transient voltages are conceivable during fault conditions with all devices and should be assumed for surge-current ratings. Different ratings may be appropriate, however, when using short-circuiters. Secondly, all operational failures are attributed (Section 5.1) to negative-resistance breakdown. Clearly there are other causes of failure, some of which could probably be traced to faults, other than local imperfections, undetected in manufacture.

I note the authors' implication that the silicon rectifier has superseded the earlier germanium type in all but a few applications.

A more standardized form of encapsulation seems necessary, particularly to reduce the disturbing variety of smaller devices.

Mr. R. A. A. Newman: A little-publicized phenomenon associated with diode surge currents is a thermal-resistance ageing effect caused by repeated overloads close to the failure point. Manufacturers' surge-current curves should therefore not be used for repetitive overloads unless specifically recommended for the purpose. Hard-soldered diodes age considerably less than the soft-soldered variety.

Section 5.4.2 infers a crest-working-voltage limitation on a 200 A diode owing to thermal stability. My company produce a similar diode rated at 1 kV peak transient voltage, 800 V maximum recurrent voltage, which when operated at a crest working voltage of 500 V gives a reverse loss of 2% of the total. It is difficult to see how this can approach the thermal-stability point when mounted on a cooling device suitable for its forward rating.

The design-approval tests of Section 5.4.7 take a considerable time to conduct, which is an embarrassment to prototype development. I have found that a temperature-cycling test, with alternate forward current heating and fan cooling periods on a 5 or 10 min cycle spanning the normal junction-temperature range, is a good 'tool' for detecting defectives quickly. Characteristics are compared before and after cycling and 1 000 cycles are adequate for an initial opinion. Despite the absence of reverse voltage, life-test failures can be predicted more quickly by this method, and *post-mortem* examination is possible since the diode has not been totally destroyed. Thermal-resistance trends can be studied for undesirable ageing. Some soft-soldered diodes cycled up to 150° C can increase S_i by two or three times in 1 000 cycles, whereas the hard-soldered variety will show no increase in 20 000 cycles or more.

One further application to be added to the authors' list is that of diodes mounted on a water-cooled copper bar—a construction often used in electrolytic and chemical plants. Here the rectifier has truly been reduced in size to a 'bulge on the busbars'.

Mr. A. Gavrilovic: Fig. 14 shows that without a triggering signal a controlled rectifier will self break-over when a certain forward voltage is exceeded. One manufacturer states that the device should always be triggered by the gate. Another states that self break-over may safely take place below a given voltage which is somewhat higher than the maximum rated forward voltage. Can the rectifier be damaged by self break-over even if the ensuing current is limited to a safe value, and what is the mechanism of the damage?

If the damage can occur, it may be difficult to operate a long series string of controlled rectifiers. Fig. D shows a typical

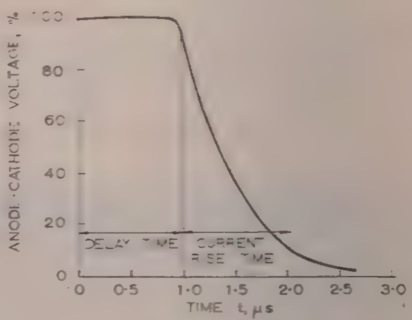

Fig. D

turn-on characteristic. If the gate triggering signal is applied at t = 0, the device will be switched in about 3μs. It may be difficult to ensure that all devices used in series are switched on simultaneously (differing gate requirements and only 1μs delay time does not give a large margin). The rectifiers may turn on sequentially, and the total string positive voltage may be applied to the last rectifier, causing its permanent damage.

Controlled rectifiers may be turned on by a rapid rate of rise of anode voltage. Therefore, the rectifiers which turn on earlier may provide the necessary rate of change of voltage to switch the remaining ones. However, the use of capacitors, for reasons given in Section 6.6.2, may slow down the rate of change of anode voltage.

Is it dangerous to apply large positive voltage, even for a very short time, to a controlled rectifier, and what precautions must be taken when operating long series strings?

Overload and surge-current rating of diodes may differ according to whether the reverse voltage is impressed after the overload or fault. In many controlled rectifier applications

there is an additional condition, namely blocking positive voltage after conduction.

As the self break-over voltage decreases rapidly above certain junction temperatures, would the authors compare the surge and overload ratings of controlled rectifiers when a positive voltage has to be held off, say, 50μs after forward conduction with the case when there is no such requirement?

Mr. W. R. E. Taylor: Can the authors give any indication of the change required in derating factors applied to parallel-connected diodes owing to manufacturing tolerances in diode fuses and voltage-drop variations between the diode base and heat sink? In my experience one can detect which diodes have been badly assembled on a heat-sink by checking their load current.

Do the authors consider that stray fluxes affect the paralleling of diodes, and do such fluxes result in an assymmetrical distribution of current in the junction with a possible increased risk of failure? In Section 5.2 I have found that current balancing under surge-current conditions is worse than at normal loads, and presumably a reduction in the resistive component (positive temperature coefficient) of forward drop would increase this unbalance, thus resulting in a larger derating factor having to be applied for parallel working.

I would like to have the authors comments on the best method of measuring individual diode currents when assembled in rectifier equipments. It seems there are three methods, namely:

(i) Using the voltage drop across shunts connected in series with each diode.
(ii) Using the voltage drop across the diode fuse (this can only apply where diodes are fused individually).
(iii) Using a tong-test type of ammeter.

I have obtained misleading readings using method (iii) including a large reverse current, no doubt owing to stray fluxes.

With regard to diodes connected in series, surely the statement at the end of Section 6.6.2 is only true if series resistors and/or capacitors are not used.

I agree with other speakers that there is no reason why medium-power diodes should be treated any differently from larger diodes with regard to crest working voltage and peak transient voltage.

Generally, small rectifier equipments are not designed to the same tolerances as large equipments, and a supplier is very seldom advised as to the possible levels of over-voltage and prospective short-circuit currents. Large rectifier equipments, on the other hand, are very carefully designed. They include surge diverters with specified impulse levels, and information is given as to supply system impedance. This means that large equipments using large diodes are much more likely to see service conditions in accordance with design parameters.

Finally, there are two general points:

(i) One manufacturer will probably have five voltage gradings for any particular diode, generally with dual polarity, each with five forward voltage-drop grades, giving 50 combinations, and it is thus important that diode manufacturers keep their various grades to a minimum.

(ii) I feel that the creepage distance on many diodes is too small for safety. For example the British Standards recommendation for creepage distance on indoor busbars is $1\frac{1}{4}$ in at 400 V r.m.s., but there are available diodes with 1 kV peak transient voltage having a creepage path of $\frac{1}{4}$ in. The British Standards figure takes into account possible transients to obtain a safe working distance, but surely the build-up of dirt and/or other conducting dust would easily be sufficient to cause flashover on the diode.

Mr. E. E. Robinson: The authors make no reference to the noise produced in associated circuits as these devices switch on or off. This noise is at radio frequencies, which fall squarely in the wavebands used by the Services. Has any work been done on designing the rectifiers so that these noises can be avoided?

Mr. P. W. Brown: I speak as a user of these devices. Have the authors had any experience of using silicon inverters working into inductive loads? Unlike the thyratron, a silicon inverter needs to have the triggering voltage maintained on each silicon rectifier while in the conducting condition, and this is not always desirable.

Mr. F. T. Bennell: I believe that there is still a big field for germanium for low-voltage rectifiers for plating and anodizing, where the voltage may be as low as 8 V. The forward voltage drop of the germanium rectifier is of the order of 0·5–0·6 V or 0·7 V, whereas with silicon it is in the range 0·9–1·2 V, and that difference of about 0·5 V makes a very great difference to the efficiency of a low-voltage rectifier.

With regard to the effect of temperature on reverse characteristics, it seems from a set of curves given by another speaker that as the temperature increases the turn-over voltage increases. The same set of curves shows an increase in reverse current with temperature. It has been suggested that the opposite is the case, and certain tests have shown that the reverse current might increase or decrease with temperature.

Mr. W. Thorn: Have the authors carried out any investigations on the use of paralleling reactors or current-balancing transformers to ensure current sharing between rectifier cells when operating in parallel?

[The authors' reply to the above discussion will be found on page 293.]

NORTH-WESTERN CENTRE, AT MANCHESTER, 14TH FEBRUARY, 1961

Mr. D. Mangnall: Owing to the very small thermal mass of the silicon diode the dissipation of losses presents a problem. For low powers natural air cooling is simple and cheap, and for larger powers forced air cooling reduces the bulk. The authors do not mention water cooling, which makes further dramatic reductions in bulk and cuts costs in large power equipments by reducing the length of conductors. The clamped form of diode rather than the screw-in type is more suitable for water cooling. It is appreciated that many electrical engineers do not favour the use of water for cooling, but there are many applications such as electro-plating and high-power electrolysis, where water is readily available, being used in fairly large quantities for processing. Over 2 MA of water-cooled semiconductor rectifiers have been installed over the past five years in some ten countries throughout the world. A further factor in favour of water cooling over air blast is that, in designing for tropical climates, one can rely more on water temperature than on ambient air temperature, which varies so much during a 24-hour cycle. Is it the authors' intention to use water cooling, and what are their views of this method which undoubtedly has many advantages?

The illustrations show the smaller diodes screwed to a heat sink, while the larger types are clamped. Many diode manufacturers use a screwed-base type for higher powers. Clamping is satisfactory if the clamping screws are evenly tightened and the surface of the diode base and heat sink is smooth and flat. Are any precautions taken in ensuring a good contact between diode and heat sink? In Fig. 4, we are presented with pictures of diodes only, without heat sink. This could easily be misleading as the heat sink normally necessary is many times the volume of the diode.

The different approach by the authors to the voltage ratings of small and large diodes in Table 1 is perplexing. The high-power diodes are operated at a crest working voltage of only half the peak transient voltage, whereas the same values are used for low- and medium-power diodes. In considering large plants utilizing high-power diodes, these are produced by a few companies who are fully experienced in the use of semiconductor rectifiers, whereas, as the authors state, the low- and medium-power diodes will no doubt ultimately take the place of many selenium-type rectifiers used in control circuits of large power equipments. Therefore, these small diodes will be used by many engineers who may have little experience of their limitations and little appreciation of the surge voltages which could be generated. The failure of a small diode may paralyze a large plant, and I would prefer to see a greater safety factor in voltage rating in smaller diodes to allow for unconscious misuse.

In Section 5.4.7 reference is made to troubles which can occur in diodes which are soft-soldered, in supplying a load involving repeated thermal cycling. Repeated heating and cooling in time affects the structure of the soft solder, which breaks away from adjoining components and so allows the junction temperature to increase and the diode fails. This is avoided if certain hard solders are employed, and their use is by no means unusual among diode manufacturers owing to difficulties in fully wetting the adjoining surfaces. Are the larger-power diodes soft-soldered, and if so, what precautions are taken against cycling loads?

Mr. J. J. Arnold: As a halfway stage towards brushless alternators of large output, a 30 MW turbo-alternator was equipped with an a.c. exciter, the output of which was rectified by static semiconductor rectifiers and fed to the rotor through the usual slip-rings, thereby eliminating the exciter commutator and its associated brushgear. The excitation was controlled by a magnetic-amplifier voltage regulator, and doubts were expressed about the effect of the inherent lack of downward forcing during instability, owing to the rectifiers forming an effective short-circuit across the generator field. Tests carried out to produce various forms of instability and fault conditions indicated that the performance was not adversely affected.

We were fortunate in being able to record some unintentional pole-slipping. As the rotor angle swings from 180° to 360°, the field current is rapidly suppressed and would be reversed if there were a path for it, e.g. through a d.c. exciter. In this case the current cannot reverse owing to the rectifiers, and a voltage appears across the rectifiers, peak inverse voltages of $3\frac{1}{2}$ times the full-load excitation voltage being recorded. This is a value which must be specified along with the usual maximum current conditions during short-circuits. At present, sets of up to 550 MW are under construction for the C.E.G.B. using a similar scheme of static silicon rectifiers. This is striking evidence of the reputation for reliability which these rectifiers have earned.

Mr. R. S. Paulden: Fig. 6 gives a peak surge-current curve for a typical 10 A cell, and shows that it will withstand 150 A peak current for five cycles. As the working peak current at 10 A mean is about 30 A this means that the cell will withstand five times the full load for 0·1 sec. Is this ratio typical also of the high-power rectifiers, or does the unconventional enclosure of the 200 A cell [Fig. 4(*h*)] give any improvement in this direction? Are the surge ratings based on comparatively rare faults, totalling perhaps a few hundred incidents in the life of the equipment, or can the surges be repeated indefinitely?

Hole-storage effects are mentioned in Sections 5.3 and 6.6.2. I understand that these effects are less marked with silicon than with germanium cells. Does the authors' experience confirm this, and is it normal practice to fit hole-storage-suppression capacitors on silicon rectifiers?

Mr. G. Buckley: I would like to put the user's point of view, in particular with reference to the protection of semiconductor rectifiers. During the past two years I have commissioned some 15 rectifiers of the semiconductor type. These have been in connection with changing over electricity intake supplies from direct to alternating current, and rectifiers have been installed in certain cases so that the existing d.c. equipment within the various works can be still used. One case where a germanium rectifier has been used was for an electrolytic load, and the rectifier was a complete success, with no attention needed since it was installed. However, with peaky loads there have been troubles with the protection. Typical of the works loads which have been catered for have been several cranes, plus miscellaneous d.c. motors of various sizes up to 40 hp on mixers, crushers, etc. We have purchased rectifiers from three different manufacturers and there seems to be no uniformity of thought with regard to the protection. Two of the manufacturers have used special quick-acting h.r.c. fuses on the secondary side of the rectifier transformer, together with a circuit-breaker on the d.c. output. A typical occurrence would be as follows: a crane driver, working on piecework and thus in a hurry, swings over his tiller control too quickly. With a mercury-arc rectifier using normal fuse protection this would have caused the circuit-breaker in the crane cabin to operate, and on resetting by the crane driver work could continue. However, with the semiconductor rectifier using the quick-acting fuses, this overload by-passes the circuit-breaker in the crane cabin, the circuit fuse controlling the crane and the circuit-breaker on the rectifier, but it blows the quick-acting fuses. The works electrician is then faced with a shut-down of the whole d.c. supplies in the factory without having any knowledge, unless the crane driver volunteers the information, of the cause of this breakdown in supply. Furthermore, these fuses are not readily obtainable, but have a delivery time of something like 14 days, and they cost 11s. 6d. each. Thus there are difficulties with this form of protection. One manufacturer has now agreed to substitute conventional h.r.c. fuses instead of the quick-acting type, but is allowing this by changing the germanium cells to silicon cells. I understand that silicon has a higher thermal capacity than germanium. Another manufacturer produced a similar type of rectifier with no protection whatsoever other than normal h.r.c. fuses on the a.c. input to the rectifier.

Mr. C. Stewart: The company with which I am associated is actively pursuing the application of silicon power rectifiers to 'brushless excitation' of large generator units. Progress has of necessity to proceed in stages, not only because we are working with radial accelerations of the order of 3 000*g* and 4 500*g* at 3 000 and 3 600 r.p.m., respectively, but because the rectifier assembly for an output of over 2 MW at 1 min ceiling condition, complete with fuses and hole-storage capacitors, has to be packed into the minimum space possible, ventilated adequately and proved for reliability.

Various operating hazards have to be allowed for, and two examples with a bearing on Fig. 6 are as follows: (*a*) The sudden short-circuit of the main generator, which could cause a peak transient current of about 2·8–3·3 times full-load excitation current or 4 times if measured over the peaks of the superimposed 50 c/s ripple. If the field were suppressed under this condition, the peak voltage could be about 1·8–2 kV. (*b*) The possible failure of a leg circuit of the 3-phase-bridge rectifier arrangement, which, with the usual sub-transient reactance of an a.c. exciter, could cause a short-circuit current of about three times the full-load excitation current. Another hazard is pole-slipping: when a rectifier-excited machine pole-slips, its field

current is momentarily reduced to zero, giving rise to a maximum induced voltage across the rectifiers of about 3·5 times the full-load voltage for a machine with a synchronous reactance of 2·0 per unit. These hazard values of currents and voltages are easily calculable, fortunately, but still leave the problem of accommodating the precautionary measures which are indicated.

[The authors' reply to the above discussion will be found on the next page.]

SOUTH MIDLAND CENTRE AT BIRMINGHAM, 1ST MAY, 1961.

Dr. W. G. Thompson: The semiconductor rectifier brings to the notice of electrical engineers facets of chemistry and solid-state physics not normally encountered in engineering work.

In view of the large efforts expended in parallel by independent manufacturers on the problems of research and development of these devices, one is tempted to speculate whether from the national interest there might not have been scope for a combined or co-ordinated programme in this field. It is considered by some manufacturers, both in this country and abroad, that the future of the semiconductor power devices lies so much with silicon that the expenditure on developing both silicon and germanium power rectifiers is not warranted.

The manufacturing techniques enable the necessary high degree of perfection to be attained in the structure of the devices, but the physical phenomena where the junction is exposed at the surface will always be present. In view of the high reverse voltages quoted, has any attempt been made to measure the field strength across this region? Surface treatment is also important, and in this, opinions differ as to whether to etch, varnish or merely rely upon encapsulation in an inert atmosphere.

Silicon diodes of extraordinary reliability have been produced and proved in service, and therefore I would welcome clarification of the authors' remark about thermal runaway being the 'usual cause of failures in properly designed equipment'.

Comments upon shelf life and stability of characteristics in service would be of interest, and is there any significance in the degree of 'roundness' of the turn-over point in the reverse current characteristic?

It has been found that water cooling can be of advantage in some applications, although I gather that air cooling can meet most requirements.

Having regard to the fact that the majority of applications will be in the industrial voltage range, is there any justification for research and development into extending the range of the reverse voltage to much higher values?

Again, one hears from time to time of large-area diodes being offered with current ratings much in excess of those in general use; are there practical limits in this direction?

Major G. I. Young: Could the authors comment on the sensitivity of the silicon rectifier to trigger electrode over-voltage?

Mr. Sarjant also contributed to the discussion at Birmingham.

THE AUTHORS' REPLY TO THE ABOVE DISCUSSIONS

Messrs. A. J. Blundell, A. E. Garside, R. G. Hibberd, and **I. Williams** (*in reply*): Several speakers question the two systems of reverse voltage rating. Very-high-voltage rectifiers are designed to avalanche between 1·5 and 2kV. If the rectifier is run at its highest possible temperature the turnover is usually soft and the limitation is due to power loss in the reverse direction. The optimum c.w.v. may then be well below the avalanche voltage, and the p.t.v. and m.r.v. ratings, which imply a negligible power loss, can be much higher than the c.w.v. It is then desirable and logical to have two separate ratings. In small-area rectifiers avalanching is often very sharp, and if the maximum temperature is separately determined, i.e. by the breakover in controlled rectifiers, the power loss just below the avalanching point is low and the maximum c.w.v. and p.t.v. ratings are practically identical. However, in this case we believe that it is good practice to give a recommended operating c.w.v. at a suitably lower value for the guidance of the average user. If high-voltage surges are not suppressed the p.t.v./c.w.v. ratio must be increased.

A valuable contribution concerns energy limitation for p.t.v. In the case of the m.r.v. there must be at least a limit to the average power dissipated, which will be a suitably small proportion of the loss due to the c.w.v. It would seem desirable to consider these limits for the I.E.C. specification.

We agree with Mr. Coleman that higher voltages have been achieved, but only as specially selected items. High-power devices with a higher c.w.v. are not in use in significant quantities.

To Mr. Bennell, we would say that if the reverse current below avalanching decreases with temperature this is probably due to high surface leakage.

Mr. Coleman and Dr. Thompson question that all operating failures are attributable to negative-resistance breakdown. Ignoring such gross defects as, for instance, a piece of conducting material short-circuiting the junction or mechanical failure, we would be interested to learn of any failure not caused by localized thermal turnover, unless the rectifier runs away owing to improper cooling system design.

Messrs. Boul and Coleman query the figure for reverse loss. For normal rectifier operation we see no reason for an appreciably smaller value. However, this figure is for maximum working temperature and c.w.v.; the average working loss would be very much less. For the 200 A rectifier mentioned in the paper the reverse loss under maximum conditions is about 3% of the forward loss.

It is fortuitous that a calculation of fin design based upon eqn. (6) of the paper usually gives a stable result, but this approach can be misleading. Often a rectifier is downrated and used with a small fin when space is at a premium. Then the stability point can be approached.

As Mr. Boul suggests, it is occasionally necessary to increase the number of parallel cells to cope with fault current. The overload characteristics of the small and large cells are similar, but as the spreading resistance is not proportional to area the larger cells are less favoured. However, the limit of area has not yet been reached by cells in present large-scale production.

The temperatures of Table 1 have something in hand for short-time overloads.

The effect of cyclic loads on soldered joints is very complex. The so-called hard solders have melting points above about 300°C. Thus the frozen-in stress when the rectifier is taken to low temperature is increased. This is evident by the reduction in surge-current rating which some manufacturers make at low temperatures. The most desirable solder has a high tensile strength, but a melting point not much above the working temperature of the rectifier.

Silicon imperfections shown by Mr. Langridge can reduce the reverse voltage. Anomalous results can be obtained with

specific samples, but over large quantities we agree with his comments. Techniques are sufficiently advanced to allow a specification which rejects material not completely free from this type of defect. Only material with a low density of evenly-distributed etch pits would be accepted.

Epitaxial growth from the vapour phase may be a substitution for crystal growing which Major Peter anticipates.

It has been predicted that silicon would entirely supersede germanium, but now that the two can be compared on their own merits, it is seen that the earlier view was incorrect. Silicon is at a disadvantage to germanium in medium- and low-voltage heavy-current applications, especially where high efficiency is required. This is not to decry its advantages in fields where it is suited.

Water cooling seems unnecessary for general use. It complicates the equipment and does not increase the fault-current rating. Where it is desirable to avoid corrosive atmospheres a closed-cycle air-cooled system with a water-cooled heat-exchanger has frequently been employed. An advantage is that if the water system fails the equipment can continue to function on an open air cycle until the water system has been repaired.

Mr. Taylor mentions parallel sharing. With a separate connection lug the voltage variation is that of a normal bolted contact. In smaller devices where the electrical and thermal contacts are combined more care is necessary.

The resistance of the rectifier fuse should be controlled, but in high-power equipment current sharing is more affected by inductive phenomena in the various circuits. Cubicle design and the layout of connections must be carefully arranged to give the correct string currents. It is not thought that fluxes can cause an asymmetric distribution of current in the junction. Sharing under surge current is a very important factor. The voltage drop across the fuse can be used with fair accuracy to estimate current sharing, but constancy of fuse resistance must be assured. The statement at the end of Section 6.6.2 referred to operation without series sharing devices.

We agree with Mr. Taylor's comments about creepage distance on rectifier insulators, for there is no doubt that many devices have been offered for heavy industrial work with quite inadequate tracking distances. This allows a very neat and compact capsule, but does not give the heavy-plant user the safety factor he needs.

On the question of forward breakover in controlled rectifiers the problem is similar to that in the reverse direction. The onset of avalanching indicates a very high electric field. If the resulting current flow is anywhere localized a negative-resistance failure can be initiated, generally at the surface. Thus breakover will not be dangerous if avalanching in the bulk is lower than at the surface, and vice versa. In practice, it is difficult for the user to determine which is which. If forward blocking occurs immediately after forward conduction, conditions are similar to the reverse case with the additional hazard of reduced breakover voltage caused by the temperature due to forward heating.

Mr. Brown is incorrect in stating that the controlled rectifier needs to have the triggering voltage maintained during the conduction period. Once triggered it will continue to conduct until the current drops below a certain holding value. In reply to Mr. Robinson, r.f. noise is produced only with controlled rectifiers working with delayed firing, and is similar to that with grid-controlled arc rectifiers. In reply to Mr. Buckley, we do not believe in using fuses to protect rectifiers against d.c. faults in the general case. String fuses are only used to disconnect faulty cells so that operation can continue. Normal circuit faults are cleared by the a.c. circuit-breaker.

Dr. Thompson mentions roundness at the turnover point. If surface effects are absent the degree of roundness depends upon non-multiplying impact ionization (see Reference 9 of the paper).

The surface field strength is not easily measured but may be estimated by noting the breakdown of certain gases at various pressures.

Research into high reverse voltages must be considered in conjunction with the characteristics of known surge-absorbing devices. In the absence of efficient surge absorbers, rectifiers with p.t.v.s above 2 kV would materially assist the design of medium-voltage equipment.

Current ratings will increase gradually with time, but we feel it is necessary to have a minimum rate of production in order to control production factors. Small-quantity production of very large rectifiers will not give a guaranteed low operational failure rate very quickly.

Mr. Paulden asks if surge ratings are based on repeated faults. In general, the cell will stand repeated faults at least as well as the associated equipment.

Hole-storage effects are only slightly less marked in silicon compared with germanium, and hole-storage capacitors must be fitted.

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INFLUENCE OF HUMIDITY ON THE BREAKDOWN VOLTAGE OF SPHERE-GAPS AND UNIFORM-FIELD GAPS

By E. KUFFEL, M.Sc., Ph.D., A.Inst.P.

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SUMMARY

Studies were made of the influence of humidity on the breakdown voltage of gaps in air. The voltages were measured for gaps between uniform-field electrodes and between spheres. The sphere-gaps comprised 2, 6·25, 12·5 and 25cm-diameter spheres which were enclosed in an air-tight chamber in which the humidity was varied between 0 and 17mm Hg of water-vapour pressure. An increase in breakdown voltage of up to 5·5% was observed for some gaps when the humidity was increased. The change in voltage for a given change of humidity varied with gap length and with the shape of the electrodes. It was largest for uniform-field electrodes and decreased with decreasing sizes of sphere. With sphere-gaps the effect increased with gap length, reached a maximum at a spacing which depended on the sphere diameter and then decreased to about half the maximum as the gap was increased still further. The results were obtained with sphere-gaps which were varied up to a maximum length approximately equal to the sphere diameters.

(1) INTRODUCTION

Earlier experiments by Ritz[1] showed that the breakdown voltage of a 1cm gap with uniform-field electrodes in air at atmospheric pressure increased by about 2% when the water-vapour pressure was increased from 10 to 25mm Hg. With 2cm gaps the change was 3% when the humidity was increased from 5 to 15mm Hg.

More recently Köhrmann[2] measured the breakdown voltage with uniform-field electrodes for dry air and for air containing 10mm Hg of water vapour at a total pressure of 760mm Hg. He found that the change in breakdown voltage when the humidity was increased from 0 to 10mm Hg increased slightly with gap length. It was 2·4% for a 0·5cm gap and 3·2% for a 2cm gap. He also made measurements at a reduced total pressure of 500mm Hg and found that a humidity of 14mm Hg increased the breakdown voltage by about 6·3% for a 1cm gap. Schröder[3] used 4 and 5cm uniform-field gaps and also reported an increase in the breakdown voltage with an increase in the humidity of the air.

Little information is available concerning the effect of humidity on the sphere-gap breakdowns. B.S. 358[4] states that the breakdown voltage is independent of the humidity of the atmosphere, provided that no dew is deposited on the electrodes.

Lewis,[5] using 2cm-diameter spheres, found that the breakdown voltage of a 0·4cm gap increased by 0·13% per mm Hg of water vapour present in the atmosphere.

The purpose of the present work is to investigate the humidity effect between 0 and 85% relative humidity using spheres of various diameters. Secondly, it is required to establish whether the effect can be neglected in the accurate calibration of the breakdown voltages of sphere-gaps; or alternatively to determine appropriate correction factors. Thirdly, it is intended to correlate the present data with those for uniform-field gaps. Finally it is hoped to explain the effect in terms of the known theories of spark discharge.

(2) APPARATUS AND PROCEDURE

(2.1) Gap Arrangement

The parallel-plane electrodes and the 6·25, 12·5 and 25·0cm-diameter spheres were mounted with a vertical axis and the lower electrode was earthed. The gap assembly was enclosed in a chamber 8 × 8 × 10ft high. The plane electrodes were made to Bruce's profile[8] with an overall diameter of 14cm. They were adjusted so that their flat surfaces were parallel to each other when the electrodes were nearly touching, which condition was checked optically, and the mechanism was such that the parallelism was preserved at other gap settings. The adjustment of the gap was effected by an electric motor controlled from outside the chamber. The setting arrangement was calibrated with block gauges and was found to be accurate to within ±0·10% for spacings above 0·5cm.

The spheres were made of copper and the parallel planes of brass. The electrodes were normally polished with metal polish and washed with ether.

The clearance to neighbouring objects for the parallel planes and the 12·5 and 25cm-diameter spheres was set by the walls of the chamber. For the 6·25-cm-diameter spheres the clearance was reduced by enclosing the gap in a wire-mesh cylinder whose radius was 9·8 sphere diameters. The cylinder was earthed.

Gaps between 2cm-diameter spheres were investigated in a sealed glass chamber of 15in diameter and 2ft high. The inside of the chamber was painted with an aqueous suspension of graphite and was subsequently baked in an oven at 200°C for several hours. The spheres were mounted with a vertical axis and the lower electrode was earthed.

The gap was set with a micrometer screw and was accurate to 0·01mm. The gap could be reset without disturbing the internal atmospheric conditions.

The breakdown values for all gap lengths with a given humidity and a given shape of electrode were obtained in sequence without disturbing the chamber (any considerable error in the humidity measurements would result in a shift of the set of points on the breakdown/humidity curves).

(2.2) Humidity Control and Measurement

When the experiments were carried out in the large chamber the humidity was partially controlled by the use of saturated solutions of various salts. Alternatively, for very low humidities, dry chemical salts were used.

The large volume of the chamber made it a tedious task to control the humidity accurately with salt solutions. It was found more convenient to measure the humidity at frequent intervals and to carry out the breakdown tests when the humidity was stable or changing very slowly. The humidity was measured with a dew-point hygrometer which consisted of a small aluminium cup with a portion of the outer surface flattened and

Dr. Kuffel was formerly in the Research Department, A.E.I. (Manchester) Ltd., and is now at the Manchester College of Science and Technology.

polished to a mirror finish. The cup was normally filled with ether and cooled by blowing air through the ether at a rate sufficient to produce the required cooling. For very low humidities the ether was cooled by adding small pieces of solid carbon dioxide. A beam of light was directed from outside the chamber through a Perspex window and into the mirror of the hygrometer. The reflected beam fell on to a photo-multiplier. On reaching the temperature at which dew appeared there was a decrease in the photo-current owing to the decrease of the reflected light. A current reduction of about 1% indicated the onset of dew. The dew-point temperature was measured with a thermocouple, one junction of which was soldered directly on to the inside surface of the reflecting mirror.

Preliminary investigations showed that with this arrangement the water-vapour pressure could be determined to an accuracy of $\pm 0 \cdot 1$ mm Hg over a wide range of humidities.

The humidity was measured at the beginning and at the end of a test run, which included the complete series of gaps between a given pair of electrodes. In general the humidity was allowed to stabilize overnight, after which it remained constant throughout the test run to within $\pm 0 \cdot 1$ mm Hg.

The small chamber used for 2 cm-diameter sphere-gaps was first evacuated and a predetermined amount of water vapour was introduced. The vapour pressure was measured with a manometer containing a low-vapour-pressure oil (10^{-7} mm Hg at room temperature). Dry air was admitted to the chamber until the total pressure reached the atmospheric value. Dry air was obtained by slowly passing atmospheric air first through a flask containing phosphorus pentoxide and then through two U-tubes connected in series and immersed in a mixture of solid carbon dioxide and acetone.

(2.3) Voltage Measurement and Procedure

Measurements were made with both direct and alternating voltages. Descriptions of the voltage supplies and the measuring techniques have been given in an earlier paper.[7]

The direct voltage was stable to within $\pm 0 \cdot 1\%$ with a superimposed ripple of less than $0 \cdot 1\%$ of the mean value.

The gap was subjected to 15 breakdowns and the mean value was taken as the breakdown voltage. The interval between successive breakdowns was about 20 sec. Preliminary investigations showed that no change in breakdown voltage was observed during the first 150–200 sparks except in very humid air where the initial results were somewhat erratic but stabilized after about 5 to 10 breakdowns. Provision was made for cleaning the electrodes with a chamois leather from outside the chamber without disturbing the gap or the humidity condition.

The measured direct-voltage results for spherical electrodes could be reproduced to within $\pm 0 \cdot 3\%$. In any given set of results the measurements were consistent with each other to within $\pm 0 \cdot 2\%$, with the exception of those for very humid air and for certain gap lengths which will be discussed later.

With uniform-field electrodes the voltage measurements were consistent to well within $0 \cdot 1\%$ and the reproducibility of results was within $\pm 0 \cdot 2\%$.

(2.4) Other Measurements

The measured breakdown voltages were adjusted to a standard barometric pressure of 760 mm Hg and a temperature of 20° C. When tests were made in the large chamber, the temperature was measured with thermocouples situated in different parts of the chamber at the level of the gap and was accurate to $0 \cdot 1$° C. In the small chamber the temperature was measured with a mercury thermometer fixed to the inside wall of the chamber.

The pressure was obtained from a barometer situated in the laboratory. The difference between the atmospheric pressure and the pressure in the chamber, measured with an oil manometer, was added to the barometric pressure.

The gaps were very weakly irradiated by placing $0 \cdot 5$ mg of radium in the hollow shank 24 in from the sparking electrode. In the investigation of the 2 cm-diameter spheres the gap was irradiated by placing $0 \cdot 5$ mg of radium on the top of the chamber about 10 in away from the gap.

(3) RESULTS

(3.1) Uniform-Field Electrodes

Fig. 1 illustrates the percentage increase of breakdown voltage of $0 \cdot 5$, 1 and 2 cm gaps when the humidity was changed from 0 to 17 mm Hg. (A similar sequence was obtained for $1 \cdot 5$,

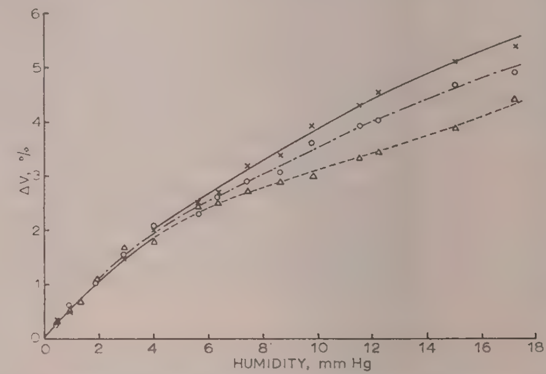

Fig. 1.—Increase in direct breakdown voltage with humidity for $0 \cdot 5$, 1 and 2 cm gaps in a uniform field.

△ $0 \cdot 5$ cm gap.
○ 1 cm gap.
× 2 cm gap.

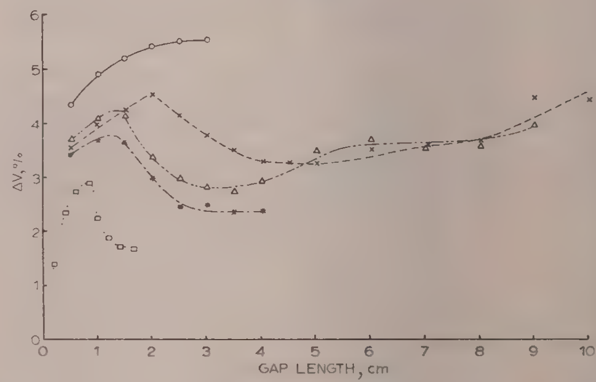

Fig. 2.—Increase in direct breakdown voltage for different gap lengths when humidity was changed from 0 to 17 mm Hg.

□ $2 \cdot 0$ cm-diameter spheres.
✱ $6 \cdot 25$ cm-diameter spheres.
△ $12 \cdot 5$ cm-diameter spheres.
× 25 cm-diameter spheres.
○ Plane electrodes.

$2 \cdot 5$ and 3 cm gaps, and some of the results are included in Fig. 2.) The quoted partial pressure was an adjusted value which gave the same proportion of water to air at a total pressure of 760 mm Hg as that measured during the actual tests.

The breakdown voltages in dry air are included in Table 1 together with the corresponding values obtained recently by Köhrmann.[2] When measurements were made in the large chamber it was difficult to obtain completely dry air and the lowest water-vapour pressure was usually a fraction of one

Table 1

BREAKDOWN VOLTAGES FOR UNIFORM-FIELD ELECTRODES IN DRY AIR AT 20° C, 760 MM HG

Gap length	Breakdown voltage	
	Present values	Köhrmann's values
cm	kV	kV
0·5	16·40	16·4
1·0	30·00	30·1
1·5	43·14	42·9
2·0	56·00	55·8
2·5	68·75	
3·0	81·30	

millimetre of mercury. The voltages for dry air were determined by extrapolating the plotted curves to zero humidity (e.g. Fig. 3 for a 1 cm gap).

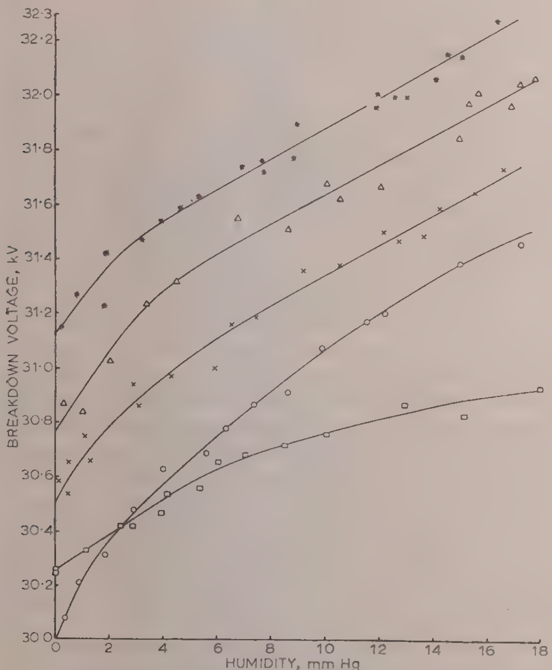

Fig. 3.—Increase in direct breakdown voltage with humidity for a 1·0 cm spacing with different sizes of electrodes.

□ 2 cm-diameter spheres.
✻ 6·25 cm-diameter spheres.
△ 12·5 cm-diameter spheres.
× 25 cm-diameter spheres.
○ Plane electrodes.

(3.2) Spherical Electrodes

The change in breakdown voltage when the water-vapour pressure was increased varied with the sphere diameter and with the sphere spacings. The experimental results for direct voltages are plotted in Fig. 2, which shows the increase in the breakdown voltages for all the gaps investigated. When the water-vapour pressure was changed from 0 to 17 mm Hg with spherical electrodes the humidity effect increased with gap length until a maximum effect was reached at a well-defined gap length. Further increases in the gap length gave a reduced effect, but the larger spheres gave an effect which again increased with still longer gaps. The observed maximum change was smallest with 2 cm-diameter spheres, increased with the size of the spheres, and was largest for uniform-field electrodes.

For spacings smaller than the sphere radius the actual breakdown voltages agreed with those quoted in B.S. 358 within the limits of the humidity effect. For larger spacings the present results were lower, but it was shown that the difference was due to different proximity conditions. The effect of proximity for sphere-gaps has been studied separately.[18]

Fig. 3 illustrates the variation of the breakdown voltage for 1 cm gaps between spheres of all sizes and between uniform-field electrodes over the whole range of humidity. The results were less consistently reproduced for sphere-gaps than for uniform-field electrodes. They were reproducible to about $\pm 0{\cdot}3\%$ with a consistency for a given set of results within $\pm 0{\cdot}2\%$. Above 85% relative humidity (e.g. 18 mm Hg at 23° C) in some experiments, the results became erratic, giving a scatter of several per cent, but this could be reduced by wiping the electrodes with chamois leather and sometimes by repeated sparking between the electrodes.

Except on 2 cm-diameter spheres the 1 cm gap gave results which were in the region where the humidity effect increased with the gap length. In this region the breakdown-voltage/humidity curves of Fig. 3 display the same trends. The initial rate of increase of the breakdown voltage was comparatively rapid, but at and above 3–4 mm Hg of water vapour the rate of change was nearly constant. The latter result is in agreement with earlier observations.[1]

Gap lengths greater than those which gave a maximum effect of humidity resulted in curves of breakdown-voltage/humidity having an appreciable curvature over the full range of humidities. This is illustrated by the curve for a 1 cm spacing between 2 cm spheres (Fig. 3) and by the curves of Fig. 4, which apply to a 2·5 cm spacing with other sphere sizes. The curve for the 12·5 cm-diameter spheres (2·5 cm spacing) shows a discon-

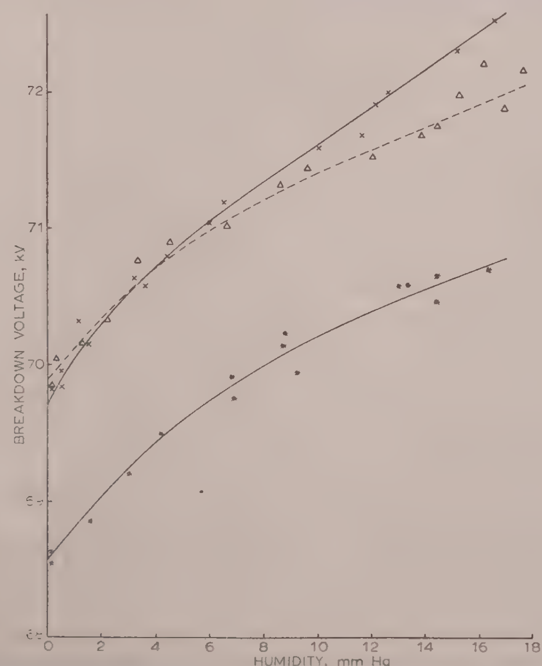

Fig. 4.—Increase in direct breakdown voltage with humidity for 2·5 cm spacing with 6·25, 12·5 and 25 cm-diameter spheres.

✻ 6·25 cm-diameter spheres.
△ 12·5 cm-diameter spheres.
× 25 cm-diameter spheres.

tinuity at 3–4 mm Hg water vapour. Furthermore, the breakdown values in the region of high humidity were more scattered than for other gaps. A similar discontinuity and a larger scatter were observed for 6·25 cm-diameter spheres with a 2 cm spacing. The phenomenon is discussed further in Section 4.2.3.

Thus, the relationship between the breakdown voltage and the humidity between 4 and 17 mm Hg was substantially linear for spacings less than that which gave the maximum humidity effect. The relationship was less linear with larger spacings. Curves giving the humidity effect for a change from 4 to 17 mm Hg of water vapour have been drawn in Fig. 5.

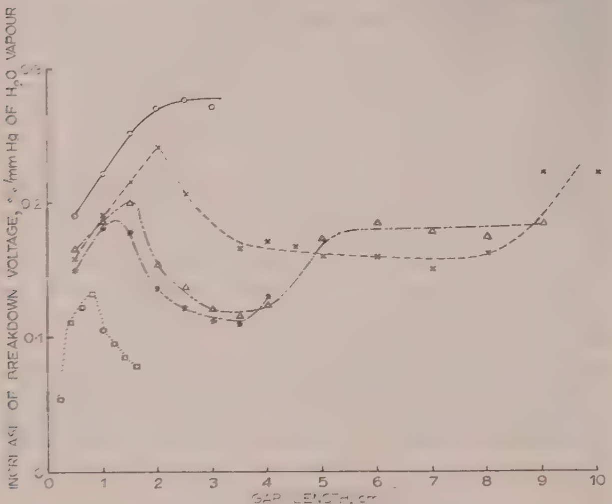

Fig. 5.—Percentage increase in direct breakdown voltage with humidity for different gap lengths and sphere sizes in the range of humidity 4–17 mm Hg.

2·0 cm-diameter spheres.
6·25 cm-diameter spheres.
12·5 cm-diameter spheres.
25 cm-diameter spheres.
Plane electrodes.

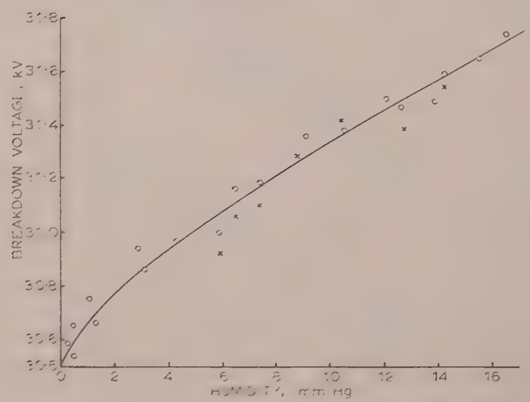

Fig. 6.—Comparison of the influence of humidity with direct and alternating voltages for 1·0 cm gap between 25 cm-diameter spheres.

○ Direct voltages.
× Alternating voltages.

Assuming the breakdown-voltage/humidity relationship to be linear in this range, the curves can be used to obtain interpolated values. (The maximum error introduced by the assumption of linearity was 0·5% of the breakdown voltage.)

The effect of humidity on the breakdown voltage with 25 cm-diameter spheres was also studied with alternating voltages over the range 6–15 mm Hg of water vapour. In general the alternating breakdown voltages were slightly lower than the direct ones but the difference did not exceed 0·5%. Fig. 6 gives the two sets of results for a 1 cm spacing. The measurements of the direct and alternating breakdown voltages were made with the same atmospheric conditions and without disturbing the gap setting.

(4) DISCUSSION

(4.1) Uniform-Field Gap

The increase of the breakdown voltage with the quantity of water vapour in the atmosphere may be explained partly by considering the higher electron attachment in moist air. The problem has recently been discussed by the author[9] and by Prasad and Craggs.[10] If electron attachment is included, the Townsend expression for current in the gap before breakdown occurs is modified and becomes[6]

$$i = i_0 \frac{\dfrac{\alpha}{\alpha - \eta}\varepsilon^{(\alpha-\eta)d} - \dfrac{\eta}{\alpha - \eta}}{1 - \gamma\dfrac{\alpha}{\alpha - \eta}[\varepsilon^{(\alpha-\eta)d} - 1]} \quad . \quad . \quad . \quad . \quad (1)$$

where α and γ are the Townsend's primary and secondary ionization coefficients and η is the attachment coefficient defined by analogy with α as the number of attachments per electron per centimetre path in the field direction.

At the breakdown condition the denominator of eqn. (1) is zero, giving

$$\frac{\alpha}{\alpha - \eta}\gamma[\varepsilon^{(\alpha-\eta)d} - 1] = 1 \quad . \quad . \quad . \quad . \quad (2)$$

Prasad and Craggs showed that the rate of increase of the attachment coefficient is considerably greater than that of the ionization coefficient as the partial pressure of water vapour is increased. In addition, the secondary ionization coefficient, γ, was found to decrease with increasing water-vapour pressure. In consequence an increase in breakdown voltage is required to attain the same ionization efficiency. Estimation of the increase in the breakdown voltage with humidity for uniform-field electrodes, made[9] on the assumption that the higher attachment in moist air is responsible for the higher breakdown voltage, gave a reasonable agreement with the observed values over a range of gaps from 0·5 to 2·0 cm.

An increase with spacing in the voltage change for a given humidity change also follows from electron-attachment considerations. The ionization coefficient, α, varies more rapidly with the field gradient than does the attachment coefficient, η (see Table 2). The field gradient required for the breakdown decreases with increasing gap length (Table 1), and consequently the influence of η on the ionization efficiency will be greater for longer gaps.

The flattening of the curve (Fig. 2) at the 3 cm spacing may be due to the field becoming appreciably non-uniform at this gap length.

The experimental results are in reasonable agreement with the early results of Ritz[1] and the dry-air values agree well with the recent results of Köhrmann.[2]

(4.2) Humidity Effect with Sphere-Gaps

(4.2.1) Smaller Gaps giving Less than the Maximum Effect.

The character of the family of curves showing the increase in breakdown voltage with humidity (Fig. 2) resembles the inverse of the curves for the irradiation effect.[7] Fundamentally

Table 2

VARIATION OF THE FIELD GRADIENT AT THE BREAKDOWN VOLTAGE ALONG A GAP OF 1·0 CM BETWEEN 6·25 CM SPHERES

	Humidity	E	α	η
	mm Hg	kV/cm		
6·25 cm-diameter spheres				
Distance from cathode:				
0		34·5	35·45	5·22
0·25 cm		30·8	21·4	4·80
0·5 cm	0	29·6	16·35	4·70
Plane electrodes ..		30·0 (uniform)	18·6	4·81
6·25 cm-diameter spheres				
Distance from cathode:				
0		35·4	39·1	9·55
0·25 cm	12·5	31·6	24·6	8·43
0·5 cm		30·3	19·8	8·06
Plane electrodes ..		31·24	23·0	8·32

Actual breakdown values taken from Fig. 3.

the two effects act in opposition; thus the additional irradiation augments the background current and thereby reduces the breakdown voltage.

It was suggested[7] that the augmented background current caused field distortion owing to the presence of space charge and thus lowered the breakdown voltage. On the other hand, the water-vapour molecules of humid air capture a portion of the free electrons with the formation of negative ions and this process increases the breakdown voltage. The irradiation effect was largest for the smallest spheres and it decreased with the larger sizes of spheres. The reduction in the breakdown voltage for an irradiated gap was more pronounced for the smaller spheres with the higher current density which occurred in the gap between them.

The breakdown-voltage/humidity results (Fig. 2) for sphere-gaps display three characteristic features. First, the breakdown voltage increases with the partial pressure of water vapour. Secondly, the total voltage change for a given humidity change increases with gap length, and thirdly, the humidity effect increases with the size of spheres and is largest for uniform-field electrodes. The first two features are in common with the features of the uniform-field gaps discussed in Section 4.1. Similar arguments show that the effects may be attributed to the relative values of the ionization, and attachment coefficients in air of different humidities and at different voltage gradients. The increase of the humidity effect with the size of spheres may be explained by considering the variation of the ionization and attachment coefficients along the axis of the sphere-gaps. The field gradient has a minimum value in the middle of the gap. The field distribution along the axis of the gap may be calculated from the expression[11]

$$E = \frac{2d[d^2(f+1) + 4y^2(f-1)]V}{[d^2(f+1) - 4y^2(f-1)]^2} \quad . \quad . \quad . \quad (3)$$

where

$$f = \frac{\frac{d}{r} + 1 + \left[\left(\frac{d}{r}+1\right)^2 + 8\right]^{1/2}}{4}$$

d = Gap length.
r = Sphere radius.
y = Distance of the point under consideration from the mid-point of the line joining the centres of the spheres.
V = Potential difference between the spheres.
E = Voltage gradient at distance y.

The variation of the coefficient α with voltage gradient along the axis of the gap is proportionately greater than that of η. As an example, Table 2 shows the distribution of the two coefficients along a 1 cm gap between 6·25 cm spheres. The field gradients were calculated using eqn. (3). The humidities at which the breakdown voltages were taken were zero and 12·5 mm Hg. The corresponding values of α and η were measured by Prasad and Craggs in dry air and in air with 12·5 mm Hg of water vapour.

Neglecting the contribution by the secondary ionization coefficient the gas-amplified current in the gap is given by

$$i = i_0 \varepsilon^{\int_0^d (\alpha - \eta) dx} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (4)$$

so that the exponent of this expression is changed by the unequal changes of α and η across the gap. This leads to differences between the conditions of the currents in the uniform-field gap and sphere-gaps, respectively, such that for the latter α has a relatively greater influence, so that a change of humidity (or η) causes less change in voltage than would be the case with a uniform-field gap.

(4.2.2) Gap Length with Maximum Humidity Effect and a Comparison with the Irradiation Effect.

The breakdown mechanism for sphere-gaps depends on the gap length. Two mechanisms were suggested by Loeb and Meek.[13, 14] In short gaps with a uniform or nearly uniform field at the minimum breakdown voltage an electron avalanche crosses the full gap from cathode to anode. In longer gaps, when the field becomes non-uniform, the field strength in the middle of the gap may be too low for the electron avalanche to propagate across the full gap. The field strength will be highest in the vicinity of the high-voltage electrode and the breakdown will be initiated near that electrode. At the breakdown voltage Meek[14] proposes a gradient sufficiently high to give transition from an avalanche to a streamer which in turn propagates across the gap to form a conducting filament between the electrodes.

A previous analysis[7] was made of the distribution of α across the critical gaps which gave the maximum irradiation effects for spheres of different sizes. This showed that the minimum value of α in the middle of these gaps remained approximately the same for all spheres. It was suggested that the second breakdown mechanism may have operated with gaps greater than those which gave the maximum effect.

The influence of humidity on the irradiation effect for gaps between 6·25 cm-diameter spheres is demonstrated in Fig. 7. The gaps were irradiated from 0·5 mg of radium placed inside the hollow high-voltage sphere. The addition of water vapour to the air diminished the critical gap length and the magnitude of the maximum irradiation effect. The experiments of Prasad and Craggs[10] show that the increase in the attachment coefficient, η, was greater than that of the ionization effect, α, when the humidity was increased (e.g. the addition of 2·5 mm Hg of water vapour to 150 mm Hg of dry air increased the attachment coefficient by a factor of approximately 1·5, but no noticeable change was observed in the value of α). In consequence the effective value of $\alpha - \eta$ at the centre of the critical gap for dry-air conditions would be reduced when the air was humid and the applied voltage was the same. Thus the new critical gap would be slightly reduced, giving a more uniform distribution of voltage, and the voltage would have to be increased so that these combined changes resulted in the same critical value of $\alpha - \eta$ which was required for the change of the breakdown mechanism.

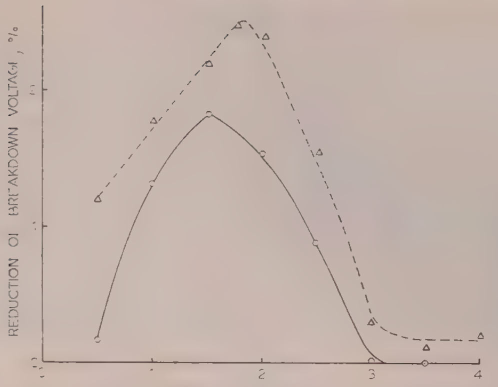

Fig. 7.—Reduction of direct breakdown voltage of 6·25 cm diameter sphere due to gap irradiation at two humidities.

Gap irradiated with 0·5 mg of radium behind the sparking electrode of 1 mm thickness.

△ Humidity 4 mm Hg.
○ Humidity 13 mm Hg.

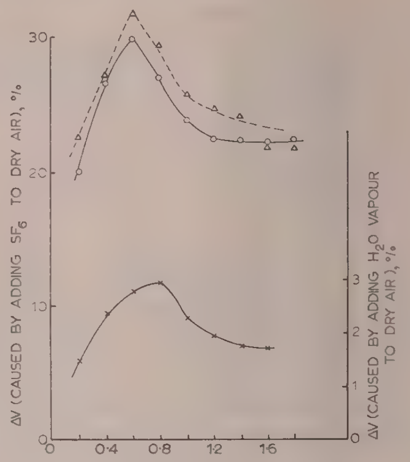

Fig. 8.—Increase in breakdown voltage by adding sulphur hexafluoride or water vapour to dry air. 2 cm-diameter spheres.

○ Direct voltage in mixture of 6·5% SF_6 and 93·5% air.
△ Alternating voltage in mixture of 6·5% SF_6 and 93·5% air.
× Direct voltage in mixture of 17 mm Hg of water vapour and air.

The distribution of the coefficients α and η along the axis of the gaps which gave maximum humidity effect (Fig. 2) was made using the most recent Prasad and Craggs values for α and η. The values for maximum and minimum field strengths determined by using eqn. (3), together with the corresponding values for α and η, are included in Table 3.

Table 3

VARIATION OF THE IONIZATION AND ATTACHMENT COEFFICIENTS ALONG THE AXIS OF THE GAPS WHICH GAVE MAXIMUM HUMIDITY EFFECT

Sphere diameter	E_{max}	E_{min}	$(\alpha - \eta)_{max}$	$(\alpha - \eta)_{min}$
cm	kV/cm	kV/cm		
2·0	36·4	28·6	34·6	6·9
6·25	34·9	28·7	26·5	7·0
12·5	33·55	28·34	22·7	6·2
25·0	30·6	28·6	12·5	6·9

It is seen that, although $(\alpha - \eta)_{max}$ varied considerably for different sizes of spheres, $(\alpha - \eta)_{min}$ in the centre of the gap remained nearly the same in all cases. The maximum humidity effect occurred with gaps which were shorter than those for the corresponding maximum irradiation effect (e.g. for 6·25 cm-diameter spheres the critical gap was reduced from 1·75 to 1·25 cm). However, the irradiation experiments were carried out in an atmosphere with only 4 mm Hg water-vapour pressure and consequently the effect of electron attachment on the breakdown voltages was smaller in these experiments.

A further experiment was made to demonstrate the influence of electron attachments. Dry air was mixed with sulphur hexafluoride gas in the proportion 93·5% to 6·5% sulphur hexafluoride by volume. The latter has a very high cross-section for the formation of negative ions in the region of low electron energy.[12] The breakdown voltage in the mixture was studied using 2 cm-diameter spheres with direct and alternating voltages. Fig. 8 compares the results with those for moist air. The ordinates show the percentage increase in the breakdown voltage for different gaps above the corresponding values in dry air. The addition of the electronegative gas gave a large increase in the voltage and also shortened the critical gap for the maximum effect. The alternating breakdown voltages in the mixture of air and sulphur hexafluoride were consistently higher than the corresponding direct-voltage values, but the difference did not exceed 2·5%.

(4.2.3) **Humidity Effect with Longer Gaps.**

Earlier results[15] show that the breakdown voltages for sphere-gaps could be fitted into two curves with a discontinuity of slope at their point of intersection. The gap lengths at which the discontinuity occurred depended on sphere diameter. Other workers[16] reported a larger scatter in the region between $\frac{1}{2} d_k$ and d_k, where d_k is the gap length corresponding to Toepler's discontinuity.

In the present investigation a larger scatter of results was observed in the region where the humidity effect decreased as the gap length was increased. The effect was particularly noticeable for 6·25 cm diameter spheres with a 2 cm spacing and for 12·5 cm diameter spheres with a 2·5 cm spacing. It is possible that in this region either mechanism may operate.

The gap lengths which gave the maximum humidity effect were smaller than those observed for Toepler's discontinuity. Köhrmann[2] recently studied the formation of streamers in humid and dry air and he observed that the over-voltage required for the transition from avalanche to streamer is much smaller in humid air than in dry air. As a consequence the transition from Townsend's to a streamer breakdown mechanism in humid air would occur at a smaller spacing than in dry air.

From Fig. 2 it is seen that with the larger spheres and the longer gaps the humidity effect again increased. With long gaps the field strength becomes less uniform and it is more difficult to evaluate the results quantitatively. However, the humidity effect in this region may be caused by a decrease in the secondary ionization coefficient, γ, with increasing gap length. This results from the increased absorption of photons by water-vapour molecules; the problem has been recently discussed by Meek.[17]

Finally, there appears to be no significant difference in the humidity effect with direct and alternating voltages. Fig. 6 shows that, at least in the region between 6 and 15 mm Hg of water vapour, the direct- and alternating-voltage results agree within the experimental errors. However, the addition of sulphur hexafluoride to dry air caused a 32·5% increase in the alternating breakdown voltage, as compared with 30% increase with direct voltage.

(5) CONCLUSIONS

The present investigations show that the humidity of the air has a significant effect on the breakdown voltages of sphere-gaps and uniform-field gaps at atmospheric pressure. The voltages for uniform-field gaps up to 3 cm were 4–5·5% higher when the humidity was increased from 0 to 17 mm Hg of water-vapour pressure. The change in voltage was not linearly related to either the humidity for a given gap length or the gap length for a given humidity. However, the voltage change was nearly linearly related to the humidity between the conditions 4–17 mm Hg when the gap was constant. Between these humidity limits the voltage increase was greater for longer gaps (Fig. 5), giving 0·19%/mm Hg for a 0·5 cm gap and 0·27%/mm Hg for a 2 cm gap. The increase was tending to level out at 0·28%/mm Hg for still longer gaps, which were then at the useful limit for the uniform-field electrodes employed in this investigation.

With sphere-gaps the influence of humidity varied with the sphere diameter and also with the spacing. It was greatest for the largest spheres and decreased with decreasing size of sphere. Within the limits 4–17 mm Hg of water-vapour pressure the voltage-change/humidity relationship for a constant gap was nearly linear (similar to the uniform-field gaps). The change in voltage for a given change of humidity increased with gap length, reached a maximum value at a well-defined spacing and then decreased to about half the maximum as the gap was further increased. For the larger spheres the effect again increased at still longer gaps. The maximum effect for the 2 cm-diameter spheres was about 0·13%/mm Hg of water-vapour pressure and for the 25 cm-diameter spheres it was 0·24%/mm Hg.

It has been shown in an earlier paper[9] that the increase in the breakdown voltage with humidity may be explained in terms of the higher electron attachment in the humid air. The variation of the humidity effect with gap length could be related to the distribution of the ionization and attachment coefficients across the sphere-gap. An analysis of the distribution of these coefficients showed that the maximum effect for different sizes of sphere occurred when the minimum values of the effective ionization coefficient, $(\alpha - \eta)_{min}$, in the middle of the gaps were approximately the same. It was suggested that at spacings longer than that which gave the maximum humidity effect a second breakdown mechanism may operate.

The variation of the humidity effect with gap length and with the size of the spheres makes it impracticable to quote a simple relationship numerically between voltage change and humidity. Approximate correction curves which give the percentage increase in voltage per millimetre of mercury of water-vapour pressure have been plotted for the humidity range 4–17 mm Hg (Fig. 5).

Brief checks were made with 50 c/s alternating voltages. In the range 5–15 mm Hg water-vapour pressure the direct and alternating values agreed with one another within the scatter of the results.

(6) ACKNOWLEDGMENTS

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[The discussion on the above paper will be found on page 314.]

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THE INFLUENCE OF NEARBY EARTHED OBJECTS AND OF THE POLARITY OF THE VOLTAGE ON THE DIRECT-VOLTAGE BREAKDOWN OF HORIZONTAL SPHERE-GAPS

By E. KUFFEL, M.Sc., Ph.D., A.Inst.P., and A. S. HUSBANDS, B.Sc.(Eng.), Member.

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SUMMARY

Experiments were made to determine the influence of nearby earthed objects on the breakdown voltages of 6·25 and 12·5 cm-diameter sphere-gaps. The effect was found to be a function of both gap length and sphere diameter. A method has been discussed for establishing clearances which are suitable for use when the sphere-gaps are employed for accurate voltage measurements. Recommended clearances have been quoted which are suitable for all sizes of spheres, and a distinction has been made between the clearance conditions required for experimental calibrations of the breakdown voltages, and those required when a calibrated gap is used for the measurement of voltage.

The polarity of the direct voltage had only a small influence on the breakdown voltages of the gaps.

(1) INTRODUCTION

Little quantitative information is available concerning the influence on the breakdown voltage of earthed objects surrounding the sphere-gap.

B.S. 358[1] gives rules for the measurement of voltages with sphere-gaps and specifies minimum and maximum distances from the gap to an earthed plane to which one sphere is connected. Let D be the sphere diameter, S the gap length, and A the distance between the sparking point of the h.v. sphere and the earthed plane, when the latter is behind the earthed sphere and perpendicular to the axis of the gap. Then for spheres of diameters between 2 and 25 cm the specified limits* for gaps with $S > 0 \cdot 5D$ are $5D < A < 7D$, and for gaps with $S < 0 \cdot 5D$ the limits are given as $10S < A < 7D$. For larger spheres the quoted limits as a function of D decrease as the sphere diameter increases. The specification applies also to a horizontal arrangement of the spheres and their shanks, the common axis of which is parallel to an earthed plane or the floor. The clearance in this case is measured from the sparking point of the h.v. sphere to the floor.

In addition, B.S. 358 specifies the minimum distance from any conducting body to the sparking point of the h.v. electrode. This minimum clearance was given by $(0 \cdot 25 + V/300)$ metres, where V is the voltage measured in kilovolts (peak). There was an additional proviso that the clearance shall not be less than the sphere diameter.*

The corresponding German (VDE) specification[2] calls for a minimum clearance to surrounding objects which is quoted in terms of the gap length and the distance to an earthed plane. The minimum clearance is given by $(A - S)$.

The present investigations were intended to establish a suitable standard of clearance which could be adopted when other variable factors were investigated. It was also intended to determine approximately the change in breakdown voltage which could be expected if the clearance was varied from an arbitrary standard value.

Hueter[3] investigated the effect of an earthed metal cage surrounding the gap and found that the breakdown voltage could be lowered appreciably, particularly when the gap length exceeded the sphere radius.

In the present experiments the gap was enclosed in a cylindrical cage. Cylinders of various diameters were used and the breakdown voltages were measured for gap lengths up to a sphere diameter. Additional measurements were made with an earthed conducting plane parallel to the axis of the gap. The plane was so arranged that the distance between the gap and the plane could be varied.

Investigations were also carried out into the effect of polarity on the direct-voltage breakdown of sphere-gaps. This is also discussed in References 4 and 5. B.S. 358: 1939, recommended the use of impulse-voltage calibrations for the corresponding polarities for direct-voltage measurements. With small gaps the calibration Tables showed no significant difference between the positive and negative impulse breakdown voltages, but for sphere spacings greater than a sphere radius and less than a sphere diameter the positive impulse voltage exceeded the negative value by as much as 10%. Subsequent to the publication of B.S. 358 in 1939 it was shown[4,6] that this difference was greatly reduced by the use of adequate irradiation for spheres of less than 25 cm diameter.

The American Standard[10] A.I.E.E. No. 4 (1940), and its subsequent editions (1943 and 1953), recommend the use of power-frequency calibrations for direct voltages of either polarity, which assumes that there is no polarity effect.

(2) APPARATUS AND MEASURING PROCEDURE

(2.1) Gap Arrangement

In the study of the proximity effect of an earthed object, 6·25 and 12·5 cm diameter spheres were used. The spheres were arranged with a horizontal axis, and the gap assembly was enclosed coaxially in a cylindrical wire-mesh cage of radius B, which has been taken as the radial clearance. A sketch of this arrangement is included in Fig. 1. The depth of the cage was $8D$ and the distance from the sparking point of the h.v. electrode to the metal end-plate, A, was $6D$. The cage was earthed, and the rim adjacent to the h.v. sphere was fitted with a metal ring having a circular cross-section 4·3 cm in diameter. Alternatively, an earthed plane was placed below the gap (as shown in Fig. 5) at a distance A from the gap axis, and this distance has been taken as the clearance to the plane. With both arrangements the gap between the spheres was adjusted by moving the earthed sphere along the common axis. The experiments were carried out in the normal laboratory atmosphere. The spheres were made of copper and their surfaces were polished with metal polish and

* These clearances were specified in B.S. 358: 1939. The present issue (1960) gives slightly modified values.

Dr. Kuffel was formerly and Mr. Husbands is in the Research Department, A.E.I. (Manchester) Ltd. Dr. Kuffel is now at the Manchester College of Science and Technology.

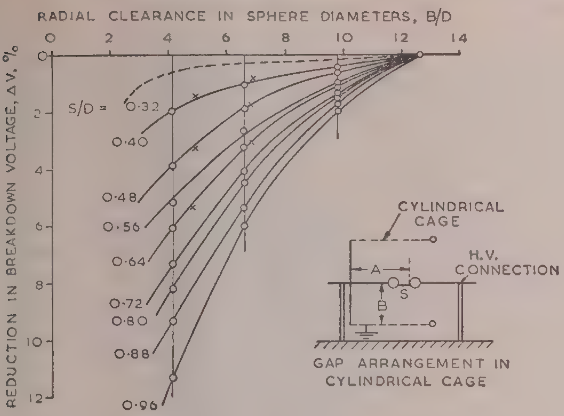

Fig. 1.—Change in breakdown voltage of 6·25 and 12·5 cm-diameter sphere-gaps with radial clearance.
○ 6·25 cm-diameter spheres.
× 12·5 cm diameter spheres.
Gap arrangement in cylindrical cage.

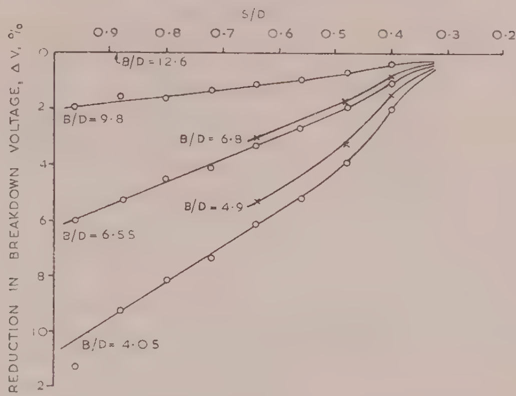

Fig. 2.—Change in breakdown voltage with S/D for different radial clearances B/D. 6·25 and 12·5 cm-diameter spheres.
○ 6·25 cm-diameter spheres.
× 12·5 cm-diameter spheres.

washed in ether. The results were found to be reproducible to within a voltage range of 1·0% from day to day. The gap lengths were measured with block gauges.

During the polarity investigations the gap was enclosed inside a chamber, 8 × 8 × 10 ft high, which has been described in Reference 7. The factors which might influence the breakdown voltage were kept constant. The humidity was controlled by the use of saturated solutions of chemical salts.[8]

(2.2) Voltage Measurement

The equipment for supplying and measuring the direct voltage has been described earlier.[7] The voltage was stable to within ±0·1% over short periods. In general, the results for the proximity investigations are considered to be accurate to within ±1·0% of the quoted values, but for comparison purposes the accuracy was considerably better. For the polarity investigations under controlled atmospheric conditions the accuracy for comparison purposes was considered to be within ±0·1%.

During an experiment and after the first trial breakdown of the gap, the voltage was set to approximately 1% below the breakdown voltage. It was then slowly increased by means of a fine control, so that the changing voltage could be followed by manipulating the dials of a measuring potentiometer. A series of 20 breakdowns was made and the mean value was taken as the breakdown voltage.

When the experiments were carried out in the open laboratory, the breakdown voltages remained consistent for about 100 sparks. Thereafter the results showed a considerable scatter unless the gap was recleaned. In practice, the gap was cleaned after each series of 20 readings.

No irradiation was used other than the natural background which prevailed in the laboratory.

(3) RESULTS

(3.1) Proximity Results

Fig. 1 shows the measured reduction in breakdown voltage for different sphere spacings, S, when the radial clearance was varied over the range from approximately 12·6 to $4D$. A radial clearance of $12 \cdot 6D$ has been taken as the reference point. The sphere spacings were in the range $S/D = 0 \cdot 4$–$0 \cdot 96$. The measured points have been replotted in Fig. 2 with different scales, and smooth curves have been drawn to average the experimental points. The dotted curve for $S/D = 0 \cdot 32$ of Fig. 1 was obtained by extrapolating the curves of Fig. 2.

The experimental values for a cylindrical cage and for a given S/D fitted closely into an empirical relationship of the form

$$\Delta V = m \log \frac{B}{D} + C \quad . \quad . \quad . \quad . \quad . \quad (1)$$

where ΔV is the percentage change in breakdown voltage from the value when the clearance was $12 \cdot 6D$, and m and C are factors which depend on the ratio S/D. This relationship is shown in Fig. 3, where the observed changes in the breakdown voltages

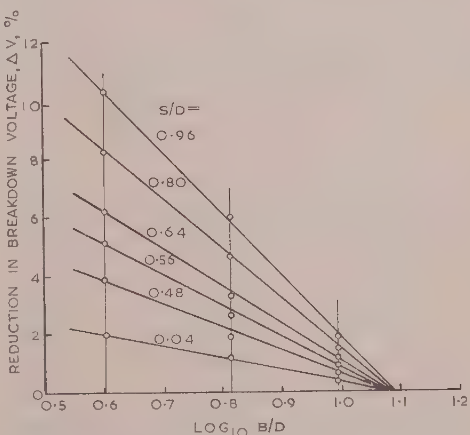

Fig. 3.—Relationship between the change in breakdown voltage and log (B/D) for different values of S/D.
$\Delta V = m \log_{10}(B/D) + C$

for different values of S/D are plotted against log (B/D). The relationship is reasonably linear in the range of the present investigations.

Fig. 4 shows that the relationship between m and log (S/D) was approximately linear between the limits $0 \cdot 3 < S/D < 1 \cdot 0$. Below $0 \cdot 3$ the effect has been taken to be negligible.

Eqn. (1) then becomes

$$\Delta V = 21\left(2 \log_{10}\frac{S}{D} + 1\right)\left(\log_{10}\frac{B}{D} - 1 \cdot 09\right) \quad . \quad . \quad (2)$$

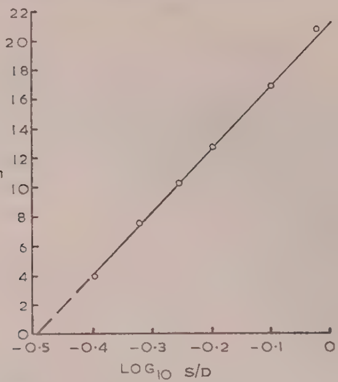

Fig. 4.—Values for the gradients m of the lines of Fig. 3.

$$m = k \log_{10} (S/D) + C_1$$
$$k = 42 \cdot 4 \quad C_1 = 21$$

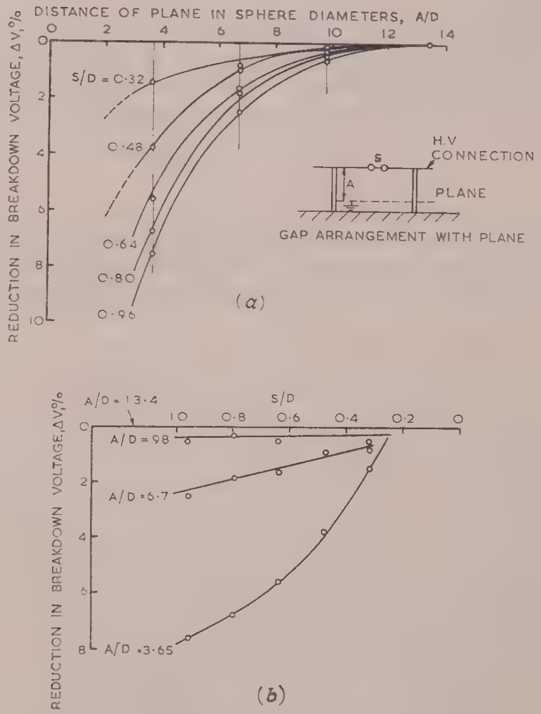

Fig. 5.—Change in breakdown voltage with clearance to a plane parallel to the gap axis for 6·25 cm-diameter spheres.

(a) Effect of varying A/D for several values of S/D.
(b) Effect of varying S/D for several values of A/D.

Applying this expression to the present results gave breakdown voltages which were correct to within 1% (relative to the reference value for a clearance of 12·6D). A change in the gap limits would modify the expression slightly.

Fig. 5 shows the lowering of the breakdown voltages when an earthed plane was placed parallel to the gap axis. The clearance, A, to the plane had a very small effect when the distance was 10D or greater. Thus at a clearance of 9·8D the breakdown voltage was reduced below that for an arbitrary reference clearance of 13·4D by 0·2–0·7%, which was a little greater than the range of scatter of the results. The number of experimental points with the plane arrangement was limited, and it was not easy to fit the results into any simple relationship. A comparison of the curves in Figs. 1 and 5 for sphere spacings larger than the sphere radius showed that the ratio of the changes in the breakdown voltage, ΔV, caused by a cylindrical shield and by an equally distant plane, respectively, varied from about 1·1 to 1·8 when the clearance was varied from 4 to 12D. The ratio increased with an increase in the gap length.

Investigations were made into the influence of an earthed plane which was perpendicular to the axis of the gap and behind the l.v. electrode. 6·25 cm-diameter copper spheres were used, and the gap was arranged with its axis horizontal. The gap was not enclosed in a wire-mesh cylinder. The earthed plane was a wire-mesh plate, 2 × 2 m, and the distance from the h.v. electrode to the plane was 14D, which was equal to the distance from the axis of the gap to the floor. The gap was varied over the range of S/D from 0·40 to 0·96. The presence of the earthed plate had no measurable effect on the breakdown voltage.

In another experiment the sphere-gap was enclosed in a coaxial earthed cylinder of radius 9·8D. The breakdown voltage was investigated with an earthed metal plate perpendicular to the axis of the gap and placed inside the cylinder at a distance of 6·5 or 3·5D away from the h.v. electrode. No appreciable change in the breakdown voltage was observed for either position of the plane.

(3.2) Polarity Results

A set of results obtained with 6·25 cm spheres with both polarities is included in Table 1. The results are compared with the negative polarities which are recommended by the International Electrotechnical Committee (I.E.C.). (Other tests have shown that the difference in the breakdown voltages for the two polarities was the same with and without additional irradiation, and the difference did not exceed 0·5% in either case.)

Table 1

Direct Breakdown Voltage for Positive and Negative Polarities of 6·25 cm-diameter Sphere-Gap

Spacing	Negative polarity	Positive polarity	Difference between positive and negative polarities	New I.E.C. values (−ve)	Difference from I.E.C. (−ve value)
cm	kV	kV	% of −ve polarity	kV	%
1·0	31·55	31·53	under −0·10	31·9	−1·1
1·5	45·18	45·18	0	45·7	−1·2
2·0	57·95	57·98	under +0·10	58·6	−1·1
2·5	69·10	69·51	+0·59	69·6	−0·7
3·0	78·20	78·38	+0·23	79·1	−1·1
3·5	84·76	85·00	+0·28	87·5	−3·1
4·0	90·77	91·02	+0·27	94·8	−4·4
4·5	95·73	95·83	+0·10	101	−5·5
5·0	99·79	99·91	+0·12	107	−7·2
5·5	103·2	103·3	+0·10	(112)	−8·5
6·0	106·2	106·3	+0·10	(116)	−9·2

−20° C, 760 mm Hg, and 4 mm water-vapour pressure. Gap enclosed in a wire-mesh cylinder of radius 9·8D. No irradiation.

The large discrepancy between the present and the I.E.C. values at spacings greater than $D/2$ is due to the different proximities: the effect is discussed elsewhere.[9]

Experiments were also made with 12·5 and 25 cm spheres in the open laboratory. Here, although the differences between the breakdown voltages for the two polarities were more erratic, they did not exceed 1%, and there was no significant trend in the differences as the gap length was varied for a given size of sphere. The irregularities were attributed to the irreproducibility of the results in the uncontrolled atmospheric conditions.

(4) DISCUSSION

(4.1) General

The radial clearance and the clearance to an earthed plane for the reference (100%) breakdown voltages were chosen arbitrarily during the present experiments. They were approximately twice the clearances specified in B.S. 358. Under these circumstances a small variation in the clearance had a small or a negligible effect on the gap breakdown voltages. However, such clearances are inconveniently large for general use with sphere-gaps. Smaller clearances result in an appreciable lowering of the gap breakdown voltages, but then a small variation in them causes a significant change in the voltage. Thus the present results demonstrate the need for specifying carefully the permissible limits of clearance, in order to obtain consistent breakdown results in different laboratories and with different equipments. Subsequent arguments are based on the present results and are put forward as a basis for specifying suitable clearances and limits of clearance.

In practice, there will be several earthed or high-voltage objects at equal or comparable radial distances from a measuring sphere-gap. It is not feasible to prescribe the size and disposition of these objects, nor is it practicable, in general, to encase the sphere-gap in a cylindrical shield. Consequently, any influence which the radially disposed objects may have will be variable for different arrangements of the equipment, and this influence should be made small or negligible. In general, the joint effect of the radially disposed objects will be less than that of a cylindrical shield of equal radial clearance, B. Thus the present results give the maximum effect which could be expected for a given radial clearance. (The influence of discharging objects is not considered in these arguments.)

A floor or the equivalent of an earthed plane is always present in practice, and its influence on the gap breakdown voltage would be consistent and predictable if other radially disposed objects were absent or had a negligible effect.

The present experiments indicated that, when either the cylindrical shield or the earthed plane had the predominant effect, the other had little or no influence on the breakdown voltage when its distance from the gap was greater than a certain value. It will be assumed in the following discussion that the lowering of the breakdown voltage depends on either the presence of the plane alone or the cylinder alone, whichever has the predominant effect when both are present.

The current specifications for sphere-gaps appear to permit the same variations in the clearances during the experimental calibrations of the gap voltages and when the sphere-gap is used for voltage-measurement purposes. However, the best accuracies depend on the consistency of the calibration conditions, which should be specified within close limits which allow no more variation than, say, 1% in the breakdown voltage of a given gap. Thereafter the clearance limits could be relaxed, depending on the required accuracy when the sphere-gap is used for voltage measurement.

(4.2) Effects of Earthed Plane

Consider the influence of an earthed plane alone, for which the results are given in Fig. 5. Assume that the breakdown voltages have been accurately calibrated for a range of gap lengths, S, with a fixed distance, A_s, between the horizontal axis of the gap and an earthed plane parallel to the axis. (A_s denotes a standard value of A for the calibration.) A sphere-gap is to be used for the measurement of voltage, which is required to be correct to within a specified margin of the appropriate value of the prescribed calibration curve. There is a permissible range of the clearance, A, for the measuring gap which will comply with the accuracy requirement, and the range extends above and below the fixed value of A_s which was used for the calibration. The range of A may be determined from Fig. 5. A selection of such ranges is given in Table 2 for various gap lengths, measurement accuracies and calibration clearances.

Table 2 shows that good measurement accuracies (relative to the calibration data) are obtainable with reasonably small clearances to an earthed plane. There is a permissible variation of A for each condition of A_s, and the ranges of the variations

Table 2

LIMITS OF CLEARANCE, A, FROM THE HORIZONTAL AXIS OF A MEASURING GAP TO A HORIZONTAL EARTHED PLANE

Sphere-gap S	Voltage measurement accuracy	Clearance limits from (Fig. 5) when the standard calibration curve is taken with clearance A_s					
		$A_s = 3D$		$A_s = 4D$		$A_s = 6D$	
		A_{min}/D	A_{max}/D	A_{min}/D	A_{max}/D	A_{min}/D	A_{max}/D
	%						
$< 0 \cdot 32D$	1	1·9 (approx.)	5·6	2·3	8	3·2	∞
	2	1·2 (approx.)	∞	1·5 (approx.)	∞	1·9	∞
	3	0·8 (approx.)	∞	1·1 (approx.)	∞	1·3	∞
$0 \cdot 32D < S < 0 \cdot 5D$	1	2·4 (approx.)	3·8	3·3	5	4·9	8
	2	1·9 (approx.)	4·6	2·6	6	4·0	∞
	3	1·5 (approx.)	5·6	2	8·5	3·3	∞
$0 \cdot 5D < S < 0 \cdot 75D$	1		3·4	3·5	4·6		
	2		3·9	3·2	5·1		
	3		4·4	3	5·9		

Excluding the effects of other nearby objects.

are sufficiently wide to overlap each other in most cases. Thus, limits of A between $3D$ and $4D$ would satisfy nearly all the conditions quoted in Table 2.

The use of the larger clearances (A and A_s) allows a wider range of permissible variation of A, but it introduces practical space difficulties with the larger spheres. Later arguments which concern the radial clearance B also favour a small clearance (A or A_s). However, the breakdown voltages [Fig. 5(a)] are changing comparatively rapidly as the clearance is reduced below $A = 3D$ for $S = 0{\cdot}5D$, or below $A = 5D$ for $S = 0{\cdot}75D$. Consequently, the selection of too low a value for A_s would restrict seriously the permissible variations of A. The conflicting points indicate that A_s should be a little greater than $3D$, and a value of $4D$ seems a reasonable choice.

There is no reason why the clearance to the earthed plane, A_s, should not be $4D$ for the calibration of all sizes of sphere-gaps. It seems illogical to increase it for small spheres merely because the space is more readily available. The suggested value, $A_s = 4D$, has been chosen primarily to suit a gap length $S = 0{\cdot}5D$, but it is suitable for all gaps up to $S = 0{\cdot}75D$ and it is reasonably practical for the largest sizes of spheres. There is little or nothing to be gained by reducing A_s as the gap length, S, is reduced. Consequently, the value $A_s = 4D$ will be considered as the standard clearance to a plane for all calibration purposes, from which it follows that the relevant limits of A of Table 2 apply to all sphere sizes for measurement purposes.

The preceding arguments apply to gaps with a horizontal axis above an earthed plane which is parallel to the gap axis. The condition of a vertical gap axis and a horizontal plane has not been determined systematically during this investigation, but it is expected to give a similar trend of results, although the equivalent effects may occur with slightly smaller clearances than those quoted here.

The effects of radially disposed objects near to a sphere-gap are taken into account in the following Sections.

(4.3) Effect of Radial Clearance to Earthed Objects

Consider the influence of an earthed cylinder alone, the measured results for which are given in Fig. 1 for various clearances B. The reference clearance (100% breakdown voltage) was taken at $12{\cdot}6D$, but it is evident from Fig. 1 that further increases in B will give further changes of voltage, especially for gaps with $S > 0{\cdot}5D$. However, in practice, the radially disposed objects near to the gap will have less influence than will a complete cylinder at the same distance. Also a maximum gap of $S = 0{\cdot}5D$ is usually recommended for accurate sphere-gap measurements, so that it will be sufficiently accurate for the present purposes to take the cylinder clearance of $B = 12{\cdot}6D$ as the reference value beyond which B will be assumed to have no effect on the breakdown voltage. On this basis Table 3 has been drawn up to show the values of B which would have the same effect on the breakdown voltages as selected distances A from an earthed plane.

It is noteworthy that the ratio B/A for equivalent effects is approximately constant at $1{\cdot}35$ for $S/D = 0{\cdot}75$; it varied between $1{\cdot}1$ and $1{\cdot}5$ for $S/D = 0{\cdot}5$; and it increased progressively between $0{\cdot}6$ and $1{\cdot}4$ as the clearance was reduced with $S/D = 0{\cdot}32$ (i.e. the equivalent clearance, B, was less than the corresponding value of A with some conditions for small gaps).

Now consider that the earthed plane is the primary factor, and that it is positioned at a distance A from the gap in accordance with the values given in Table 2. The addition of an earthed cylinder has little or no effect if the radius B is equal to or greater than the appropriate value in Table 3. Thus, the required parts of Tables 2 and 3 can be combined to give limits of A and the minimum values of B which are needed for a given accuracy of voltage measurement with sphere-gaps. Table 4 gives the recommended clearances when the sphere-gap calibrations are made with the prescribed conditions.

Table 3

APPROXIMATE EQUIVALENT CLEARANCES FROM AN EARTHED CYLINDER AND FROM A HORIZONTAL PLANE TO THE HORIZONTAL AXIS OF THE GAP

Sphere-gap S/D	Clearance to a plane A/D					
	1	2	3	4	5	6'
	Equivalent clearance (radius of cylinder) B/D					
0·32	1·4	2·0	2·4	2·8	3·3	3·7
0·50	1·5	2·4	3·4	4·7	6·1	7·5
0·75	1 to 2	2·7	4·0	5·7	7·0	8·3

Table 4

RECOMMENDED CLEARANCES FOR SPHERE-GAPS (DERIVED FROM TABLES 2 AND 3)

Sphere-gap S/D	Voltage-measurement accuracy	Clearance to an earthed plane, A/D		Clearance to other objects $(B/D)_{min}$
		Min	Max	
	± %			
$S/D < 0{\cdot}32$	1	2·3	8	2·1
	2	1·5	∞	1·7
	3	1·1	∞	1·5
$0{\cdot}32 < S/D < 0{\cdot}5$	1	3·3	5	3·8
	2	2·6	6	3·0
	3	2·0	8·5	2·4
$0{\cdot}5 < S/D < 0{\cdot}75$	1	3·5	4·6	4·8
	2	3·2	5·1	4·3
	3	3·0	5·9	4·0

Calibration conditions:
Distance to earth plane, $A_s = 4D$ for all gaps
Radial clearance, $B > 2{\cdot}8D$ for $S < 0{\cdot}32D$
$B > 4{\cdot}7D$ for $0{\cdot}32D < S < 0{\cdot}5D$
$B > 5{\cdot}7D$ for $0{\cdot}5D < S < 0{\cdot}75D$

The shank of the earthed sphere (or a connecting lead) should lie along the extended axis of the gap for a distance equal to A_{min}. The lead of the h.v. sphere could diverge from the axis at a point beyond B_{min}, but if it were non-discharging, the lead could diverge from the axis at a lesser distance from the sphere, without affecting the accuracy adversely.

The values given in Table 4 fall generally within the limits suggested in B.S. 358, but in that specification the same variation of limits would be permissible during calibration.

(4.4) Polarity Effect

The results in Table 1 indicated that the direct breakdown voltage with positive polarity exceeds slightly the negative values, but the difference is very small and for most practical purposes it can be neglected.

A small polarity effect may be expected if the sphere spacing is large enough for the breakdown to take place by the streamer mechanism.[4] In the asymmetrical field with one sphere earthed (as in the present arrangement) the electric field distribution remains the same, whether the h.v. sphere is of positive or negative polarity. The avalanche initiating the breakdown for

a positive h.v. sphere propagates towards that sphere and into a region of increasing field strength, so that the criterion for the breakdown is that the space-charge field, E_a, produced by the avalanche shall equal the field E_r at the sphere surface. With a negative h.v. sphere the avalanche propagates towards a weaker field and away from the h.v. sphere surface, and the criterion for breakdown is that E_a shall equal E_{r+d}, the field at a distance d from the surface of the h.v. sphere. Thus, the breakdown for the negative h.v. sphere may be expected to take place at a voltage slightly lower than that for the positive sphere.

(5) CONCLUSIONS

The breakdown voltage of sphere-gaps was reduced when the gap was encased coaxially in an earthed cylindrical screen, particularly when the sphere spacing exceeded the radius of the given spheres. The reduction in voltage was related to the sphere spacing and the radius of the surrounding cylinder.

A similar reduction was observed when an earthed plane was used. The latter was placed below the gap and was arranged parallel to its axis. The effect of the plane was usually smaller than that of an equally distant coaxial cylinder, and at clearances to the plane greater than 10 sphere diameters it became negligible.

The investigations included 6·25 and 12·5 cm-diameter spheres, which have a similar proportional relationship between the change in breakdown voltage and the factors of clearance, gap length and sphere diameter.

The present results show that it is necessary to specify carefully the clearance to an earthed plane when accurate measurements are made with sphere-gaps. Alternatively, a correction for this clearance effect can be made by applying the present results.

The effect of objects surrounding a sphere-gap on the breakdown voltage can be made relatively small by bringing an earthed plane sufficiently near to the gap. Then the plane will have a greater and more consistent influence on the breakdown voltage than the combined effect of a number of objects which are at a comparable or greater distance from the gap.

It is pointed out that the accuracy with which voltages may be measured by a sphere-gap depends on the accuracy of the accepted calibrations. Thus the clearance conditions for the calibrations should be specified within very close limits, although the limits may be extended when the gap is used for voltage measurements. Suggested values have been given for suitable standard clearances during calibrations and for clearances which are suitable for measurement purposes (Table 4). The use of the same clearances (in terms of sphere diameters) is advocated for all sizes of spheres, and it is demonstrated that measurement accuracies of $\pm 1\%$ are obtainable with practical clearances as far as the clearance effect is concerned. The polarity of the direct voltages which were applied to the sphere-gaps had only a small effect on the breakdown voltages. Generally the voltage for a positive h.v. sphere was higher than the corresponding value for a negative h.v. sphere, but the difference was less than 1%. During tests under closely controlled conditions it was less than 0·5%.

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[The discussion on the above paper will be found on page 314.]

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THE DIRECT-VOLTAGE CALIBRATION OF AIR-GAPS IN A UNIFORM FIELD AND BETWEEN SPHERES UP TO 25CM IN DIAMETER

With Recommendations for Standard Test Conditions

By E. KUFFEL, M.Sc., Ph.D., A.Inst.P.

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SUMMARY

Direct breakdown voltages of gaps between spherical and plane electrodes in air have been measured under controlled test conditions using 2·0, 6·25, 12·5 and 25 cm-diameter spheres. The factors which influence the breakdown voltages have been studied systematically and their effect on the calibration data is discussed. Calibration Tables for sphere-gaps and uniform-field gaps are given, and the results are compared with the new values recently issued by the I.E.C.

(1) INTRODUCTION

Sphere-gaps and uniform-field gaps are often used for the measurement of high voltages. B.S. 358[1] gives calibrations for the measurement of voltages up to about 2300 kV using sphere-gaps. In the preparation of calibration Tables for sphere-gaps, Whitehead[2] analysed the results of various investigators from different countries. The majority of these results differed from each other by less than 10% and frequently the differences were less than 5%. The various results were averaged, and adjusted calibration values were chosen which were consistent between various sizes of spheres and which gave smooth voltage-spacing curves. The accuracy stated in B.S. 358 for power frequencies and impulse voltages was ±3%, while for direct voltages it was ±5%. The calibration Tables for direct voltages were based on the power frequency and impulse-voltage data.

The calibration Tables for sphere-gaps have just been revised by the I.E.C. (International Electrotechnical Commission). For comparison purposes the I.E.C. figures will be included together with the present results.

The breakdown voltages for uniform-field electrodes have been measured by several authors.[3–8] The results of various investigations differ by as much as 5% in some circumstances.

Little quantitative information is available on the variable factors which affect the breakdown voltages for sphere-gaps and uniform-field gaps, and it is likely that differences in the test conditions were responsible for some or all of the discrepancies between the measured results of different authors. A recent programme of work was designed to evaluate the influences of the variable factors, which have been discussed separately.[9–12]

This paper includes recommendations for the standard conditions of tests and measurements with spark-gaps which are based on the results of the previous investigations.

Accurate direct-voltage calibrations have been made for a range of spark-gaps under controlled conditions, and the magnitude of the changes which would occur with changed conditions has been indicated.

Dr. Kuffel was formerly in the Research Department, A.E.I. (Manchester) Ltd., and is now at the Manchester College of Science and Technology.

(2) APPARATUS AND PROCEDURE

The 6·25, 12·5 and 25 cm-diameter spheres and the uniform-field electrodes were calibrated in a metal chamber, 8 × 8 × 10 ft high, described previously.[9] The gaps were arranged with a vertical axis, the lower electrode being earthed. The plane electrodes were made to Bruce's[5] profile with an overall diameter of 14 cm. The spheres were made of copper and the plane electrodes of brass. The clearance to the neighbouring objects for the plane electrodes and the 12·5 and 25 cm-diameter spheres was set by the walls of the chamber. For the 6·25 cm-diameter spheres the clearance was reduced by enclosing the gap in an earthed wire-mesh cylinder of radius 9·8 sphere diameters.

The humidity in the chamber was controlled by the use of saturated solutions of various salts. It was measured with a dew-point-type hygrometer described in a previous paper.[10] The humidity could be measured to ±0·1 mm Hg of water-vapour pressure.

Gaps between 2·0 cm-diameter spheres were calibrated in a glass chamber 15 in in diameter and 22 in high. The inside of the chamber was painted with an aqueous suspension of graphite and was subsequently baked in an oven at 200° C for several hours. The 2 cm-diameter spheres were mounted with a vertical axis and the lower electrode was earthed. The equipment for supplying and measuring the direct voltage was described previously.[9] The voltage was stable to within ±0·1% over short periods. The superimposed ripple was less than 0·1%. In general, for given test conditions the direct-voltage calibration values for sphere-gaps could be reproduced to within ±0·3%. In any given set of results the measurements were consistent to within ±0·2%.

With uniform-field electrodes the voltage measurements were consistent to within 0·1% and the reproducibility of results was within ±0·2%.

(3) RESULTS

The measured direct breakdown voltages for sphere and uniform-field gaps are given in Table 1 together with the corresponding I.E.C. values. The results refer to standard conditions of 760 mm Hg total pressure, 20° C temperature and a humidity of 11 mm Hg water-vapour pressure. (The pressures are quoted in millimetres of mercury at 20° C.) A water-vapour pressure of 11 mm Hg corresponds to 10·85 g of water vapour per cubic metre, which is a close approximation to the standard of humidity, namely 11 g/m^3, specified in various I.E.C. recommendations relating to high-voltage test procedures. The values of Table 1 were corrected to the standard humidity condition by using factors which were determined from previous results.[10]

During the calibration the distances to earthed objects were fixed as shown in Table 1. The calibrations were made without the use of an artificial source of irradiation, with the exception of those for the 2 cm-diameter spheres. In the latter case a

Table 1

MEASURED DIRECT BREAKDOWN VOLTAGES FOR PARALLEL PLANE AND SPHERICAL ELECTRODES IN AIR

Gap length	Breakdown voltage												
		Sphere diameter, cm											
	Planes	2·0			6·25			12·5			25		
		Present	I.E.C.	ΔV	Present	I.E.	ΔV	Present	I.E.C.	ΔV	Present	I.E.C.	ΔV
cm		kV	kV	%	kV	kV	%	kV	kV	%	kV	kV	%
0·2		8·30	8·0	+3·7									
0·4		14·72	14·4	+2·2									
0·5	16·94	17·71	17·4	+1·8	17·40	17·2	+1·2	17·25	16·8	+2·7	17·16		
0·6		20·70	20·4	+1·5									
0·8		26·29	25·8	+2·4									
1·0	31·14	30·80	30·7	+0·3	31·90	31·9	0	31·70	31·7	0	31·40	31·7	−0·9
1·2		34·04	35·1	−2·7									
1·4		36·39	38·5	−2·9									
1·5	44·84	37·35	40·0	−6·9	45·75	45·5	+0·5	45·46	45·5	<−0·1	45·10	45·5	−0·9
1·6		38·32											
2·0	58·26				58·68	58·5	+0·3	58·70	59·0	−0·5	58·63	59·0	−0·6
2·5	71·60				70·28	69·8	+0·7	71·50	72·5	−1·4	71·77	72·8	−1·4
3·0	84·74				79·12	79·5	−0·5	84·20	85·0	−0·9	84·68	86·0	−1·5
3·5					86·14	87·5	−1·5	96·64	97·0	−0·4	97·56	99·0	−1·4
4·0	(111·1)				92·24	95·0	−2·9	108·7	108	+0·6	110·05	112	−1·7
4·5											122·9	125	−1·7
5·0								130·2	129	+0·9	135·4	137	−1·2
6·0								146·5	146	+0·3	160·3	161	−0·4
7·0								160·1	161	−0·6	183·6	184	−0·2
8·0								171·4	174	−1·5	205	206	−0·5
9·0								181·5	185	−1·9	224	226	−0·9
10·0											238·8	244	−2·1

$$\left(\Delta V\% = \frac{\text{Present—I.E.C.}}{\text{I.E.C.}} \times 100\right)$$

Proximity: 2·0 cm-diameter spheres: cylinder at $B = 8\cdot7D$.
6·25 cm-diameter spheres: cylinder at $B = 9\cdot8D$.
12·5 cm-diameter spheres: cylinder at $B = 9\cdot8D$.
25 cm-diameter spheres: cylinder at $B = 4\cdot9D$.

I.E.C. Proximity: 2·0 cm-diameter spheres: plane $7D < A < 9D$.
6·25 cm-diameter spheres: plane $7D < A < 9D$.
12·5 cm-diameter spheres: plane $6D < A < 8D$.
25 cm-diameter spheres: plane $5D < A < 7D$.

Conditions: 760 mm Hg, 20° C; humidity 11 mm Hg water-vapour pressure; negative polarity; no irradiation. Sphere-gaps were enclosed in earthed cylindrical shields.
Proposed I.E.C. values are for negative polarity.

capsule of 0·5 mg of radium was placed on the top of the chamber approximately 10 in away from the gap. Normally, at this distance from the gap the capsule had a negligible effect on the direct breakdown voltage, but it reduced the scatter of the measured voltages for very small gaps.

(4) DISCUSSION

(4.1) General

The measured breakdown voltages for sphere-gaps (Table 1) differ from the corresponding I.E.C. values. Generally, the present results for the smaller sphere spacings are slightly higher than the I.E.C. values, but the differences are within the limits of variation which could be expected for varying conditions of irradiation and humidity. The present results with the larger sphere spacings are lower than the I.E.C. values. The differences are particularly large for the 2 cm-diameter spheres. The following Sections show that the major part of the differences can be explained by taking account of the various factors which have an effect on the breakdown voltage.

(4.2) Irradiation

To ensure a sufficient supply of electrons, which are required for the initiation of the breakdown process, the gaps are often illuminated with ultra-violet light or are irradiated from some other electron-producing source. B.S. 358 suggests that additional irradiation should be used for voltages below 50 kV, and the American Standard[16] recommends ultra-violet irradiation for similar voltages. In a previous publication, the author[9] showed that the presence of radium reduced a large scatter of the gap breakdown voltages for very small gaps subjected to direct voltages for several seconds. The effect was caused by the initial shortage of free electrons in the small volume of the gap, so that the use of irradiation rectified this shortage and lowered the average breakdown values.

The radium also caused an appreciable reduction in the direct breakdown voltage for gap settings at and above 50 kV.[9] At spacings above 0·5 cm the influence of radium increased with gap length and had a maximum effect at a particular spacing which depended on the size of the spheres. In this region the direct and alternating voltages for successive breakdowns of a given spacing longer than 0·5 cm were quite consistent, both with and without irradiation, although the irradiation from 0·5 mg of radium gave a reduction of 0–4% in the breakdown values. It was suggested that the effect was due to field distortion caused by positive-ion space charge, which augmented the externally applied field and increased the ionization caused by collision processes.

The 0–4% reduction in voltage was caused by 0·5 mg of radium placed behind the sparking-point of the h.v. electrode. The thickness of the electrode wall was 1 mm. The ionization current in the gap (measured at 240 volts with a 2 cm gap) was about 3×10^{-11} amp. The current density is highest in the region of the strongest field, i.e. along and near the axis of the gap. With small spheres the current flow was constricted to a relatively narrow channel and the concentration of space charges was high. Consequently, the observed voltage reduction was larger for the smaller spheres.

The magnitude of the irradiation effect depended on the position of the radium, as would be expected. It decreased rapidly when the distance between the radium and the sparking-point was increased, and the effect became negligible when the distance exceeded 15 in. In practice, the gaps are sometimes illuminated from an ultra-violet lamp. In this case the ionization current in the gap at atmospheric pressure is likely to be of the order of 10^{-13} amp, so that an ultra-violet lamp is likely to have less effect than 0·5 mg of radium as used in the experiments.[9] The effectiveness of an ultra-violet lamp depends on the source output, the angle of illumination, the distance from the gap and, to some extent, on the material of the illuminated electrode. Thus the irradiation effect would be expected to vary slightly unless experimental conditions were standardized.

It is difficult at this stage to recommend standard conditions for the irradiation of sphere-gaps for calibration purposes. With radium (or other radioactive substance) the ion current is produced principally in the air, whereas the ultra-violet light generates mainly photo-electrons at the surface of the electrodes. The irradiation level in each case might be defined in terms of a prescribed current, I_0, in the gap from which an effective current density, J_0 ($=I_0$ per square centimetre), could be calculated approximately for each sphere size.

Assume that the effective area is that of a hemisphere. Approximate calculations showed that ionization currents giving a current density, J_0, less than about 10^{-13} A/cm^2 would reduce the direct breakdown voltage by less than 1%. (0·5% reduction in voltage was obtained for plane electrodes with $J_0 = 4 \times 10^{-14}$ A/cm^2.) Thus the results of the experiments[9] indicate that it should not be necessary to use irradiation to obtain consistent direct and alternating breakdown voltages for sphere spacings greater than about 0·5 cm. However, when irradiation is used the ion current density should be preferably less than 10^{-13} amp per square centimetre of exposed electrode surface in order to avoid variations of the voltage with varying conditions of irradiation. Alternatively, the ion current density should be specified, and an appropriate correction made using the previous[9] or similar results.

(4.3) Humidity Effect

According to B.S. 358: 1939, the breakdown voltage of sphere-gaps was assumed to be independent of the humidity of the air. In early calibrations of uniform-field gaps, Ritz[3] and Holtzer[6] observed that humidity caused a measurable change in breakdown voltage. Later authors[13] ignored the effect and usually the prevailing humidity was not noted.

A previous paper[10] has shown that the breakdown voltages of sphere and uniform-field gaps increased with humidity. The rate of increase was comparatively rapid when the humidity was changed from 0 to about 3–4 mm Hg water-vapour pressure. Above 4 mm Hg it was less rapid and the relationship of voltage to humidity became nearly linear. In addition, the change in breakdown voltage for a given change in humidity increased with gap length; e.g. with uniform-field electrodes it was 4·3% for an 0·5 cm gap length and 5·6% for a 3·0 cm gap when the vapour pressure was changed from 0 to 17 mm Hg.

It has been shown[14] that the increase of breakdown voltage with increasing humidity could be explained in terms of electron attachment. The higher attachment in humid air lowered the prebreakdown current in the gap and thus increased the breakdown voltage.

The experimental results of various authors for uniform-field gaps are compared in Table 2, which is arranged in order of descending voltage magnitudes.

Table 2

BREAKDOWN VOLTAGES FOR UNIFORM-FIELD GAPS

Gap length	Breakdown voltage according to:						
	Schumann[7] *p = unknown	Holtzer[6] p = 7–14	Ritz[3] p = 10	Present results p = 10	Köhrmann[4] p = 10	Bruce[5] p = unknown	Fisher[8] p = 0
cm	kV	kV	kV	kV	kV	kV	kV
0·5	17·4		17·00	16·91	16·8	16·41	
1·0	31·7	31·66	31·35	31·06	30·9	30·3	30·3
1·5				44·72		43·8	
2·0	59·6	61·20	58·7	58·10	57·6	57·04	55·7
2·5				71·40		70·10	
3·0	87	86·94	85·8	84·55		83·19	

760 mm Hg and 20° C.

*p = Water-vapour pressure in millimetres of mercury.

The humidity conditions for two of the columns of Table 2 are not known, but the comparison indicates that the discrepancies were probably caused mainly by the different humidity conditions. The present results fall between those of Ritz[3] and Köhrmann[4] under comparable humidity conditions.

For sphere-gaps the effect of humidity was the inverse of that of irradiation. Thus the voltage increased with increased humidity and the change was greatest for the largest spheres. The humidity effect varied with the gap length, which was also the case with the irradiation effect, and each effect had a maximum at a particular gap length which depended on the sphere diameter. The maximum effects were related to the distribution of the ionization and attachment coefficients across the gap. Thus, the effect reached a maximum value when the ionization factor $(\alpha - \eta)$ in the middle of the gap fell below the minimum required for the propagation of electron avalanches across the full gap from cathode to anode. It was suggested[10] that this spacing corresponded to a region of transition from Townsend's to a streamer breakdown mechanism, and the same suggestion applies equally to humidity and irradiation effects.

In general, the humidity effect decreased as the gap length was increased beyond that gap which gave the maximum effect. However, for larger spheres the effect tended to increase again when the spacing exceeded about 4 cm. The latter increase may have been caused by increased photo-absorption[15] by water-vapour molecules in the longer gaps, which in turn resulted in a reduction of the secondary ionization coefficient.

The investigations[10] showed that the maximum humidity effect for 25 cm-diameter spheres was 0·24% per mm Hg of water vapour. (This change applies to the range 4–17 mm Hg of water-vapour pressure.) The humidity conditions encountered in practice vary from about 4 to 15 mm Hg of water vapour. Hence discrepancies of a few per cent between various investigators working in different laboratories and at a different time of the year may arise if the humidity effect is neglected. Similar discrepancies may arise when different intensities of irradiation are used.

(4.4) Proximity Effect

The results of a previous investigation[12] showed that the presence of earthed objects lowered the breakdown voltage of sphere-gaps because of the resulting distortion of the field in the gap and the greater proportionate field strength in some parts of the gap. A comparatively large reduction was obtained when the longer gaps were enclosed coaxially in an earthed cylindrical shield. Replacement of the cylinder by an earthed plane at a distance equal to the radius of the cylinder had a smaller effect. However, the general characteristics of the reduction in voltage, in relation to the clearance from the cylinder or the plane, followed similar trends in both cases. In particular, the effect was large when the sphere spacings exceeded the sphere radius; e.g. for a gap with $S/D = 0\cdot80$ the voltage reduction was 1% and 6% when the clearance, a, was reduced from $A = 13\cdot4D$ to $8D$ and $4D$, respectively. Thus the change in voltage was related to the gap length (expressed in terms of sphere diameter S/D) and to the radial clearance A or B (also expressed in terms of sphere diameter A/D or B/D), where A is the distance from the gap to the plane and B is the radius of the cylinder. The rate of reduction in voltage was rapid when the clearance A or B was reduced below $3D$.

The experimental results[12] refer to a gap with a horizontal axis above a horizontal earthed plane. The corresponding effects for a gap with a vertical axis above a horizontal plane have not been studied systematically, but it has been assumed that the effect of the plane is the same in the two cases. The error introduced by this assumption is likely to be small.

It was suggested[12] that the consistency of the calibration data for sphere breakdowns, as obtained by various workers, would be improved by standardizing the clearance distances. A standard clearance of $A_s = 4D$ was suggested, which would be applicable to all sizes of spheres and to all gap lengths. This standard clearance to an earthed plane is small enough to be obtained readily in practice, and there is no reason to permit any significant variation in A_s during calibration measurements. (A small variation within a range of, say, $\pm5\%$ of A_s would be permissible.) In addition, the minimum clearances, B, to surrounding objects have been suggested for calibration pur-

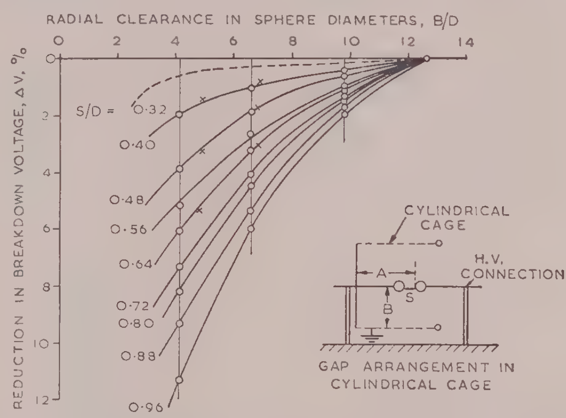

Fig. 1.—Change in breakdown voltage of 6·25 and 12·5 cm-diameter sphere-gaps with radial clearance.

○ 6·25 cm-diameter spheres.
× 12·5 cm-diameter spheres.

Table 3

Direct-Voltage Calibration Data for Sphere-Gaps with an Earthed Plane at $A_s = 13\cdot6D$

Gap length	Breakdown voltage — Sphere diameter, cm: 2·0		6·25		12·5		25	
	kV	ΔV%	kV	ΔV%	kV	ΔV%	kV	ΔV%
cm								
0·2	8·30	+3·7						
0·4	14·72	+2·2						
0·5	17·71	+1·8	17·40	+1·2	17·25	+2·7	17·16	
0·6	20·70	+1·5						
0·8	26·53	+2·8						
1·0	31·25	+1·8	31·90	0	31·70	0	31·40	−0·9
1·2	35·28	+0·5						
1·4	38·01	−1·3						
1·5	39·10	−2·2	45·78	+0·6	45·46	<−0·1	45·10	−0·9
1·6	40·33							
2·0			58·72	+0·4	58·70	−0·5	58·63	−0·6
2·5			70·65	+1·2	71·50	−1·3	71·77	−1·4
3·0			79·43	<−0·1	84·20	−0·9	84·68	−1·5
3·5			88·14	+0·7	96·64	−0·4	97·56	−1·4
4·0			94·61	−0·4	108·7	+0·6	110·05	−1·7
4·5			100·4	−0·6			122·9	−1·7
5·0			105·3	−1·6	131·3	+2·2	135·4	−1·2
5·5			110·4	−1·4				
6·0			113·5	−2·1	148·2	+1·5	160·3	−0·4
7·0					164·6	+2·2	183·6	−0·2
8·0					178·2	+2·4	206	0
9·0					189·5	+2·4	226·5	+0·2
10·0							243·8	<−0·1

$$\Delta V\% = \frac{\text{Present—I.E.C.}}{\text{I.E.C.}} \times 100$$

Pressure, 760 mm Hg; humidity, 11 mm Hg; no irradiation; negative polarity.

Table 4

DIRECT-VOLTAGE CALIBRATION DATA FOR SPHERE-GAPS WITH A PLANE AT $A_s = 4D$

Gap length	Breakdown voltage, Sphere diameter, cm: 2·0		6·25		12·5		25	
	kV	$\Delta V\%$	kV	$\Delta V\%$	kV	$\Delta V\%$	kV	$\Delta V\%$
cm								
0·2	8·30	+3·7						
0·4	14·72	+2·2						
0·5	17·71	+1·8	17·4	+1·2	17·25	+2·7	17·16	
0·6	20·43	+0·1						
0·8	25·82	<+0·1						
1·0	30·10	−1·9	31·9	0	31·70	0	31·40	−0·9
1·2	33·64	−4·2						
1·4	35·96	−6·6						
1·5	36·88	−7·8	45·78	+0·5	45·46	<−0·1	45·10	−0·9
1·6	37·92							
2·0			57·90	−1·0	58·70	−0·5	58·63	−0·6
2·5			68·83	−1·4	71·50	−1·4	71·77	−1·4
3·0			76·81	−3·4	84·20	−0·9	84·68	−1·5
3·5			84·24	−3·7	96·64	−0·4	97·56	−1·4
4·0			89·98	−5·3	107·2	−0·7	110·05	−1·7
4·5			94·6	−6·3			122·9	−1·7
5·0			99·0	−7·5	127·9	−0·8	135·4	−1·2
5·5			103·4	−7·7				
6·0			106·0	−8·6	143·1	−2·0	160·3	−0·4
7·0					157·8	−2·0	183·6	−0·2
8·0					168·2	−3·3	203·1	−1·4
9·0					178·2	−3·7	222·9	−1·4
10·0							237·9	−2·5

$$\Delta V\% = \frac{\text{Present—I.E.C.}}{\text{I.E.C.}} \times 100$$

Pressure, 760 mm Hg; humidity, 11 mm Hg; temperature, 20° C; no irradiation; negative polarity.

poses, so that these objects have an effect of less than 1% on the breakdown voltages. (The clearances, B, have been related to a standard distance $A_s = 4D$, but the same values would not be applicable if a different standard, A_s, had been selected.)

The present measurements of Table 1 were made with an arbitrary clearance to an earthed cylindrical shield around each gap. The measured voltages have been converted to the corresponding values for clearances to an earthed plane of $A = 13{\cdot}4D$ and $4D$, and the new voltages are given in Tables 3 and 4, respectively. The conversion was made with the aid of the previous results[12] in the following manner. The results were corrected to the equivalent values which would have been obtained for a common clearance to a cylinder of radius $12{\cdot}6D$, using the curves of Fig. 1. Measurements had been made previously of the comparative breakdown voltages with the cylinder ($B = 12{\cdot}6D$) and with an earthed plane at $A = 13{\cdot}4D$ for 6·25 cm-diameter spheres. The latter results were slightly higher than those with a cylinder of radius $12{\cdot}6D$. The comparative sets of values are included in Table 5.

At spacings smaller than $S/D = 0{\cdot}4$ the results for the cylinder and the plane were the same within the experimental errors. The same percentage differences have been assumed for the corresponding S/D values for other sphere sizes (D).

Thus Table 5 can be used to change the breakdown voltages from those for a cylindrical arrangement (at $B = 12{\cdot}6D$) to those for a plane arrangement (at $A = 13{\cdot}4D$), i.e. the percentage difference from Table 5 for a particular gap length, S/D, was added to the breakdown voltages for the cylinder arrangement. The modified calibration data which now apply for a plane at $A = 13{\cdot}4D$ (Table 3) are in closer agreement with the I.E.C.

Table 5

DIRECT BREAKDOWN VOLTAGES (kV) FOR 6·25 CM-DIAMETER SPHERES IN AIR WITH AN EARTHED CYLINDER OF RADIUS $12{\cdot}6D$ AND WITH A PLANE AT $A = 13{\cdot}4D$

Gap length S/D	Breakdown voltage: Plane $A = 13{\cdot}4D$	Cylinder $B = 12{\cdot}6D$	$V_{plane} - V_{cylinder}$
	kV	kV	%
0·4	70·65	70·26	0·49
0·48	79·43	79·08	0·48
0·56	88·14	86·53	1·83
0·64	94·61	92·15	2·19
0·72	100·4	97·91	2·5
0·80	105·3	102·28	2·85
0·88	110·4	106·2	3·1
0·96	113·5	109·6	3·4

Pressure, 760 mm Hg; temperature, 20° C; humidity, 11 mm Hg; negative polarity; no irradiation.

values than the measured breakdown voltages of Table 1. For example, with 2·0 cm-diameter spheres at a spacing of $S/D = 0{\cdot}75$ the measured breakdown voltage was 6·9% lower than the corresponding I.E.C. value. When corrected (Table 3), the difference for the same gap was 2·2%. Similarly a reduction in the discrepancy was obtained for the 6·25 cm-diameter spheres. The agreement between the corrected values (Table 3) and the I.E.C. ones is within 2% for smaller spacings, i.e. when

the clearance effect is small, with the exception of some gaps smaller than 1 cm. The latter discrepancy may be due to the humidity conditions of 11 mm Hg and non-irradiated gaps, while it is likely that the majority of the results adopted for the I.E.C. Tables were obtained for irradiated gaps and for varying humidity conditions.

The corrected values for the 12·5 cm-diameter spheres and larger spacings are slightly higher than the I.E.C. figures but the difference does not exceed 2·5%. With the 25 cm-diameter spheres the range of gaps investigated extended up to $S/D = 0{\cdot}4$ and little correction for clearance was required. The two sets of results agreed within 2% over the whole range of gaps. Again the 2% error is within the humidity and the irradiation-effect limits. These comparisons suggest that the I.E.C. figures for small spheres were obtained with large clearances. With larger spheres the I.E.C. figures refer to clearance limits of $5D < A < 7D$, and the results of Table 3 for 12·5 cm-diameter spheres (clearance $A = 13{\cdot}4D$) are higher than the I.E.C. values. The two sets of results would agree well if the present values were reduced to a clearance of $A = 6D$.

The modified calibration data of Table 4 apply for a clearance to an earthed plane of $A = 4D$, which was the suggested standard[12] for calibration purposes. The values were derived from those of Table 3, which were corrected to the shorter clearance

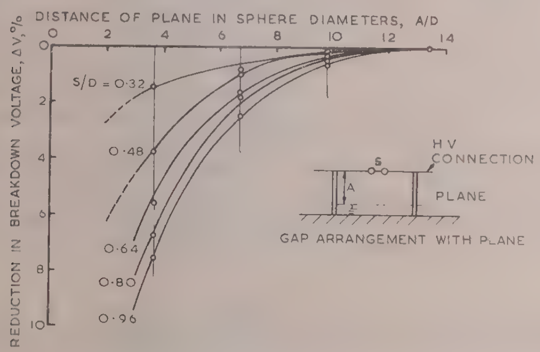

Fig. 2.—Change in breakdown voltage of 6·25 cm-diameter sphere-gaps with clearance to an earthed plane.

condition by applying the experimental results of Fig. 2. The new values for sphere spacings greater than the radius of the sphere have been reduced appreciably; e.g. for 2 cm-diameter spheres and $S/D = 0{\cdot}75$ the reduction was 5·5%.

(4.5) Secondary Effects

The variable factors which affected the breakdown voltages by about 1% or less included the initial stabilization of the gap, the materials of the electrodes and the polarity of the applied voltage. In the calibration of uniform-field gaps, Bruce[5] observed that the first few values were somewhat scattered but subsequently the results tended to stabilize at a constant value. Recent investigations showed[11] that when the gap was enclosed in a relatively small space the breakdown voltage at first increased and then became stable at a higher value after the gap had been sparked over a number of times. Initial increases of up to 1% were observed. The effect was more important with the uniform-field electrodes. It was shown[11] that the initial stabilization involved the conditioning of the electrodes and a gas conditioning effect. The gas conditioning effect was associated with the production of condensation nuclei by the sparking process. Successive measurements of the breakdown voltage and the concentration of nuclei produced by sparks showed that the two quantities were interrelated. It was suggested that the presence of nuclei in sufficient numbers modified the breakdown voltage slightly owing to the process of electron capture.

It is stated in B.S. 358 that the spheres may be made of brass, bronze, steel, copper, aluminium or light alloys. The present tests with aluminium and copper spheres showed[9] that the corresponding direct breakdown voltages agreed within 0·6%, with a tendency for the aluminium spheres to give the slightly lower values.

The present measurement[12] showed that the direct breakdown voltages for the positive and negative polarities under controlled test conditions differed by less than 0·5% over a range of gaps up to a sphere diameter, and for most practical purposes the polarity effect may be neglected.

Comparative tests were also made with direct and alternating voltages,[10] and the results showed that the breakdown values for non-irradiated gaps under controlled test conditions agreed with one another within the scatter of the results.

(5) RECOMMENDATIONS FOR THE CALIBRATIONS OF UNIFORM-FIELD GAPS AND SPHERE-GAPS IN AIR WITH DIRECT VOLTAGES

The following recommendations are based upon observations made during the course of the present investigations and they apply to measurement accuracies of ±1% for sphere spacings up to a sphere radius.

(*a*) For gaps shorter than 0·5 cm, additional irradiation should be used from a radioactive source or from some other effective ionizing source. For longer gaps (above 0·5 cm) no additional irradiation is required to give consistent results, but when irradiation is used the intensity should be carefully specified. It could be specified in terms of the ion current, I_0, per square centimetre of electrode surface, and for the effect to be less than 1% the value should be less than 10^{-13} amp per square centimetre of exposed electrode surface.

(*b*) The atmospheric humidity should be measured and the breakdown values should be reduced to a standard humidity. This may be achieved by using the present or similar correction curves (see Reference 10).

(*c*) Calibrations should be made with standard proximity conditions. The suggested standards are:

(i) An earthed plane should be placed at a distance $A_s = 4D$ (±5%) from the sparking-point of the h.v. electrode. (This recommendation refers specifically to gaps with a horizontal axis above a horizontal plane. A vertical gap axis and a horizontal plane may give results which differ by up to 1% from those quoted, but the two arrangements follow the same trends.)

(ii) All other objects (earthed or h.v.) radially disposed around the gap should be distant from the gap as follows:

for gaps with $S < 0{\cdot}32D$ $\quad B > 2{\cdot}8D$
for gaps with $0{\cdot}32D < S < 0{\cdot}5D$ $\quad B > 4{\cdot}7D$
for gaps with $0{\cdot}5D < S < 0{\cdot}75D$ $\quad B > 5{\cdot}7D$

Calibrations with proximity conditions other than the suggested standard should be adjusted to those corresponding to the standard clearance by using the appropriate experimental curves. (The clearance specifications apply to sphere-gaps; for uniform-field gaps the effect is relatively unimportant.)

(*d*) Within the limits of accuracy considered here the direct breakdown voltage is independent of the material of the electrodes and the polarity of the applied voltage.

(*e*) The surfaces of the electrodes should be smooth and free from dirt or grease. Whenever signs of deterioration occur at

the surfaces, the electrodes should be recleaned. It is advisable to ignore the results of the first few sparks between the electrodes.

(*f*) Recommended calibration data under the standard conditions are given in Table 4.

(*g*) When sphere-gaps are used for measurement purposes after calibration, conditions (*a*), (*b*), (*d*) and (*e*) also apply, but condition (*c*) may be relaxed depending on the required accuracy of measurement. Recommended conditions (*c*) are given in a previous paper.[12]

(6) ACKNOWLEDGMENTS

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DISCUSSION ON THE ABOVE THREE PAPERS AND THE PAPER BY E. KUFFEL* BEFORE A JOINT MEETING OF THE MEASUREMENT AND CONTROL SECTION, THE SUPPLY SECTION AND THE UTILIZATION SECTION, 21ST FEBRUARY, 1961

Mr. F. S. Edwards: Do the papers in any way invalidate or cast doubt upon the revised B.S. 358: 1960? The papers were written when the old standard (B.S. 358: 1939) was in force, and in at least one place the comment has been overtaken by events. For instance, on the subject of irradiation, the revised Standard states that not only should additional ionization be used at voltages below 50 kV but also it should be employed (as recommended in Section 5 of Monograph No. 329 M) with spheres of 12·5 cm diameter or less, regardless of voltage.

On the influence of humidity, Paper No. 3322 M states that with 25 cm-diameter spheres the breakdown voltage may increase by about 0·2% per gramme of moisture per cubic metre. In the laboratory with which I am best acquainted the humidity very rarely falls outside the range 4–12 g/m^3. Taking 8 g/m^3 as the mean figure, in an ordinary laboratory the effect of humidity will not be more than about 1%, which is no greater than the usual scatter of results. It was a little disconcerting to see that the effect was greatest for the largest spheres, especially as the author's results extended only up to a diameter of 25 cm, and one wonders what would happen when the diameter was 200 cm. Fortunately there is some slight evidence that with very large spheres the effect may be no greater than with smaller ones.

Some experiments carried out a year or two ago on 100 cm sphere-gaps showed that over a rather narrow range of humidity the effect, if it existed, was masked by the scatter of the results. The figures used in B.S. 358: 1960 were obtained under conditions where the humidity was not recorded and so the data could not be related to any specific humidity.

Table 1 of Paper No. 3372 M is very reassuring especially as the greatest discrepancies between the author's figures and the British Standard occur outside the range of spacings in which the British Standard is concerned about accuracy. The discrepancies with the smaller spheres are largely due to the author's use of an unirradiated gap; if irradiation had been used, agreement with the B.S.I. figures would have been much closer.

The principles given in Paper No. 3371 M are basically the same as those which were followed in the preparation of both the 1939 and the 1960 editions of B.S. 358. If external influences cannot be removed the next best thing is to control and specify them as closely as possible. The paper states that it is illogical to increase the clearances for small spheres merely because the space is more readily available. There is no question of logic about it; it is a question of practical expediency, and there is no doubt that the small sphere-gap is in general used with larger clearances relative to its size than the large sphere-gap. If we were starting all over again we might well consider adopting the

* 'The Effect of Irradiation on the Breakdown Voltage of Sphere-Gaps in Air under Direct and Alternating Voltages', *Proceedings I.E.E.*, Monograph No. 329 M, February, 1959 (106 C, p. 133).

principles in the paper; the important thing, however, is to stipulate the conditions, not necessarily to make them the same for all sizes of spheres.

Appendix B of B.S. 358: 1960 explains candidly how the calibrations were obtained: in 1939 there were two calibrations, the United States and the I.E.C. ones, with a small difference between them. The recent war prevented any further consideration of the matter at the time, but in 1956 the subject was discussed by the I.E.C. It was realized that two courses were open: either to make a complete recalibration of all sizes of spheres, which might take 10 years, or to average the two calibrations, which is what has been done. This may not have been very scientific but at least it resulted in the production of an international document which appears, from tests made subsequently, to be reasonably correct.

Dr. P. R. Howard: An investigation is being sponsored by the Central Electricity Generating Board at the National Physical Laboratory on the effect of humidity on the power-frequency flashover voltage of sphere- and rod-gaps and insulators.

This work is being carried out on practical arrangements of sphere and rod electrodes and insulators in a humidity chamber, 70 ft × 50 ft × 50 ft high, formed by screening off part of the high-voltage laboratory with a nylon sheet coated with polyvinyl chloride. Humidities up to 24 g/m^3 are obtained by mixing air with steam, and humidities less than 2 g/m^3 by replacing air in the chamber with dried air from a compressor. Condensation is prevented by heating the chamber, and results are never taken until conditions have been stable for a considerable time.

The work on sphere-gaps was undertaken primarily as a check following results with rod-gaps and insulators which showed that the humidity correction curves currently used have no general validity. 1 m sphere-gaps were used and the relationship between percentage correction and absolute humidity are shown in Fig. A. It is assumed that the correct 50 c/s breakdown voltage for a given gap is that relating to an absolute humidity of 11 g/m^3.

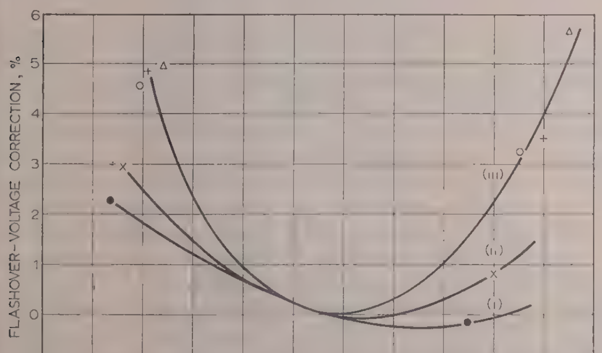

Fig. A.—Relationship between percentage correction to be applied to power-frequency flashover voltage and absolute humidity.

(i) 5 cm gap.
(ii) 10 cm gap.
(iii) 20, 30 and 40 cm gaps.

It is difficult to tie up these results with those in Paper No. 3322 M, but from the curves it appears that, had data been obtained for either 1 or 2 cm gaps, the breakdown strength may have increased with absolute humidity.

Although more work on this subject is needed, one can draw comfort from the fact that, provided that the absolute humidity lies between 8 and 16 g/m^3, significant errors are unlikely to arise with sphere-gap calibrations involving large spheres and gaps.

Dr. J. H. Mason: The authors show that the direct breakdown voltage, V_G, of sphere-gaps is increased by between 1 and 5%, depending on the humidity, the diameter, D, of the spheres, and the gap length, S. They also show that the presence of earthed conductors at a distance $4D$ from the gap may reduce V_G by up to 12%, depending on the ratio S/D. The presence of dust on the spheres may also reduce V_G to an unpredictable extent. Where accurate and reproducible results are required it seems desirable to predetermine these variables by mounting the spheres in a cylindrical case, possibly of radius $4D$ and length $10D$. I envisage walls of light aluminium sheet, with Perspex windows, covered inside with wire mesh to prevent accumulation of static charge. A cheaper construction might be possible with walls of aluminium mesh covered (outside) with transparent semiconducting plastic film. Trays of silica gel might give adequate dryness, although it would be preferable to circulate dry air.

Have the authors considered such a construction, and could they comment on the size of bushing which would be needed to mount different h.v. spheres, without danger from flashover? What are the possible effects of insulators in the neighbourhood of spark-gaps?

Mr. E. L. White: Some observations we have made of the behaviour of various 3-electrode gaps with electrode diameters of 1 to 2 cm suggest that the breakdown voltage of a sphere-gap increases with prolonged application of direct voltage. The two outer electrodes of a 3-electrode gap were maintained at +2 kV and −2 kV with respect to earth and the centre electrode. It was found that the minimum gaps which would withstand these voltages decreased over a period of hours or days, provided that no sparkover occurred during that period. Alternatively, if the gaps were not altered, the magnitude of the pulse required to trigger the gap increased by 50% or more. In total darkness, a faint glow could be seen on the surfaces of the electrodes, due possibly to electroluminescence or to radiation from ions swept from the gaps to form space charges very close to the electrodes. The accumulation of such charges might account for the increased strength of the gaps. The outer electrodes were either spherical or hemispherical and the centre electrode either spherical or conical. Has any similar effect been observed with sphere-gaps?

Mr. J. D. Endacott: In my laboratory of about 10^6 ft^3 internal volume, it is necessary to set aside permanently about 10^5 ft^3 to satisfy the clearance requirements of B.S. 358 for 200 cm sphere-gaps. The authors' work on d.c. behaviour has shown the corrections which can be applied for the effect of humidity, irradiation and the nearness of earthed objects. Although not investigated by the authors, a further correction for the nearness of live objects must be expected. Since even the 6% variation of B.S. 358 for impulse voltages is of serious importance to very-high-voltage power-cable manufacture, where the margin of breakdown over withstand voltage can never be large, the importance of the authors' work to engineers is readily understood.

The authors' most important suggestion is that by suitably placing earthed objects near the sphere-gap some control of the inter-laboratory variations is possible. Does this allow some reduction in the 6% and 10% variations quoted in B.S. 358?

There is an obvious need for some form of co-ordinating authority to standardize the reporting of research work on sphere-gaps. At present, there is great difficulty in correlating the results of investigators owing to the varying methods of information presentation and to the absence of environmental

factors which were not the immediate concern of the research worker. Standardized, comprehensive reporting would lead to a much more rapid clarification of sphere-gap behaviour.

Mr. C. G. Garton: Were the stress-distribution values shown in Fig. 5 of Monograph No. 329 M calculated or measured? If the latter, how were they measured? Spark nuclei were mentioned but not defined. What is their nature? As their numbers increase, does this increase or decrease the probability of a spark occurring?

Dr. A. Nemet: Have the authors tried to measure high voltagse with a rotating electrostatic voltmeter instead of a sphere-gap? This instrument is more convenient to use for voltage measurements in the range 10–100 kV and probably beyond.

The mechanical design of sphere-gaps should be considered more carefully. The problem is often to obtain mechanically reproducible results as well as electrically reproducible ones.

Mr. J. F. Perrin: How is the spark-gap calibrated for very high voltages?

THE AUTHORS' REPLY TO THE ABOVE DISCUSSION

Dr. E. Kuffel and Mr. A. S. Husbands (*in reply*): In reply to Mr. Edwards, the papers do not invalidate the data of B.S. 358, nor were they intended to cast doubts on that most valuable data. They were intended to evaluate the individual influences of the irradiation, humidity and proximity effects, primarily with a view to improving the accuracy and repeatability of the calibration results by imposing more closely controlled calibration conditions. Generally the present results were in agreement with those of B.S. 358, within the quoted margin of $\pm 5\%$ for direct voltages—but it was this margin with which we were concerned. We agree with Mr. Edwards that a variation of humidity between 4 and 12 g/m^3 would cause a $\pm 1\%$ variation of the mean breakdown voltage in some cases, but there are many places where the humidity falls outside this range. Paper No. 3322 M indicates that the humidity effect increased with the size of sphere, and one might suppose that it would approach that of the uniform-field gap as the sphere size was increased beyond the diameter used during the experiments. However, Mr. Edwards's experiments and those of Dr. Howard show that the present results cannot be extrapolated outside the experimental conditions. Paper No. 3371 M recommends a fixed standard clearance in terms of the sphere diameter for all sizes of sphere, which, if accepted, would dispose of one variable factor in the specification. However, this recommendation refers specifically to the conditions under which sphere-gaps are calibrated, and not necessarily to those when a calibrated gap is used for voltage measurements, so that the question of expediency need not arise.

Dr. Howard's results with 100 cm-diameter spheres are most interesting and it is unfortunate that there is no overlap of the conditions for his results and those of Paper No. 3322 M. As noted above, the extrapolation of either set of results does not give agreement with the other. Dr. Howard's results were obtained in a chamber lined with insulating material, and our results were obtained in an earthed metal-lined chamber. The insulation lining could accumulate static charges whose magnitude might decrease with higher humidities, but it seems unlikely that this effect could cause a significant change in the breakdown voltage with such a large chamber.

Dr. Mason points out that dust in the air can influence the gap breakdown voltage, and it is well known that the presence of dust will reduce the voltage. Nevertheless we would not normally advocate that a measuring sphere-gap should be enclosed in dust-free air, since this would impose great practical difficulties with the larger spheres. However, when a lower-voltage gap is required to break down consistently at a precise voltage, there would be some advantage in enclosing it in a controlled atmosphere. We would prefer a conducting or a metal enclosure rather than an insulating one near to the gap, since the latter might acquire a static charge.

Mr. White's observations are unusual in our experience, which was confined to higher voltages in general. However, somewhat similar effects were once encountered with small aluminium or magnesium spheres. These were attributed to the surface conditions of the electrodes, which, when clean and well polished, were covered by an insulating oxide layer. Then the breakdown voltage tended to be slightly lower than for other metals and it was very consistent. After much use the electrode surfaces became dull and slightly pitted, and the breakdown voltage increased and became erratic.

We sympathize with Mr. Endacott's views and consider that the space might be reduced and the measurement accuracy improved if more painstaking corrections were made to the calibration data. Such a development still requires further investigation into the characteristics of sphere-gaps and the factors which affect them.

In reply to Mr. Garton, the stress-distribution curves were calculated using Dean's formula (Reference 11 of Paper No. 3322 M). The spark nuclei referred to in Paper No. 3372 M (Reference 11) were small (radius about 5×10^{-7} cm) and were produced by sparks in air. They were detected and the concentration was measured with the gap enclosed in a small chamber. The concentration increased with repeated sparking until a saturation condition was reached at which the rate of decay equalled the rate of production. The final concentration depended on the air humidity. The nature of the nuclei is not known, but they are thought to be oxide products of nitrogen formed by the discharges, and subsequently they serve as hygroscopic centres for the formation of water molecule clusters. Experimentally the presence of the nuclei was found to cause an increase in the gap breakdown voltage, and the voltage change was related to the concentration of nuclei.

We did not use a rotating electrostatic voltmeter which Dr. Nemet mentions, and we have not encountered any serious mechanical troubles in setting sphere-gaps to the required degree of accuracy.

Mr. Perrin raises the general problem of making independent measurements of very high voltages in order to calibrate sphere-gaps. This may be done with power-frequency voltages by using an accurate voltage divider or a recognized peak-voltmeter circuit. Impulse-voltage measurements may also be made by using a voltage divider, but here the problem of accuracy becomes serious.

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THE PROTECTION OF HIGH-VOLTAGE INSULATORS FROM POWER-ARC DAMAGE

By A. E. GUILE, Ph.D., B.Sc.(Eng.), Associate Member.

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SUMMARY

This paper summarizes the results and conclusions of an experimental investigation into the reduction of damage by power-follow-through arcs on various types of high-voltage insulators. Where normally used, protective fittings of the usual type have been tested, and suggestions have been made for modifications to the fittings so as to give greater protection. The effects of wind have been taken into account. Only 11 and 33 kV insulators have been tested, but the investigation has been designed to obtain information which can be applied to the larger units at the highest voltages.

Since any application in service of these results must depend upon economic considerations, some information is included on the amount of arc damage occurring in service to high-voltage insulators, both in this country and abroad.

(1) INTRODUCTION

Suggestions were made by the E.R.A. in 1949 for an investigation into the effectiveness of arcing fittings on high-voltage insulators, and this investigation started in 1952.

The problem was originally brought to light partly in connection with cable sealing ends. For convenience and economy in setting up laboratory tests it was decided to test relatively small insulators and bushings (equivalent for this purpose to cable sealing ends), suitable for system voltages from 6·6 to 33 kV.

In service, fault currents from a few hundred amperes to 12 kA may be experienced. It was felt that it would be difficult to achieve the highest currents economically in a laboratory, and that a current range of 0·5–2 kA would give the required information. The upper limit of current available in the Short-Circuit Laboratory at Queen Mary College for arcs across insulators proved to be 1·4 kA.

Faults on supply systems are usually cleared in 0·2–2 sec and it was suggested that tests should be made with arc durations in this range. All tests on insulators were made with an arc duration of 1 sec, but further tests were made to give information on corresponding currents and arc durations for insulator fracture.

Two conditions had to be considered: (*a*) arcs following flashover due to lightning and (*b*) arcs following flashover due to surface deposits. In case (*a*) the power-follow-through arc would appear between the arcing horns or nearest metal parts and in case (*b*) it would be across the insulator surface. In both cases it was decided that the most convenient method of arc initiation would be to use fine fuse wire, and subsequent experience has confirmed that this method of artificial initiation does not affect the behaviour of the subsequent arc.

Information was needed on the ways in which wind acting on the arc could affect damage, and a wind speed range of 0–60 m.p.h. was suggested. Tests have been made over the whole of this range in a wind tunnel constructed for this investigation, with 16 in-square working section. For tests on 33 kV line insulators a second wind tunnel with 4 ft-square working section was used.

Since an arc is usually made to move on arcing fittings by the magnetic field set up by the arc current in the fittings, the first part of the investigation was concerned with the relative effects of wind and magnetic forces on the arc.[1] This served to establish the technique of testing. A drum camera with speeds of 350, 600 and 960 frames/sec was constructed and used for this and the later stages of work on insulators in order to photograph the arc movement.

11 kV bushings with a single gap and with two gaps in series of the usual types were tested, and modified fittings which gave considerably greater protection were developed.[2]

Various types of pin, post and suspension insulators and 33 kV bushings with two gaps in series were tested. Where fittings were often used in service they were tested, and modifications or additions to these were suggested where appropriate.[3]

(2) MOVEMENT OF ARCS UNDER WIND AND MAGNETIC FORCES

Steel electrodes 2 ft long and $\frac{3}{8}$ in in diameter were set up parallel and horizontal, one above the other, in a 16 in-square working section of a wind tunnel. Current feed to the electrodes was at one end only, so that the arc, initiated by fine fuse wire 2 in from this end, moved away from it under the magnetic field due to current flowing in the electrodes. This movement could be directly opposed by wind. The arc movement was photographed at speeds of 350 and 960 frames/sec.[1]

At electrode spacing between 1·25 and 7·5 in and at alternating currents up to 3 kA, an empirical relationship between arc current I, electrode spacing d (inches) and arc velocity V(ft/s) was found, namely

$$V = 0{\cdot}016\, I(5/d + 1) \qquad (1)$$

This velocity was obtained by measuring distances travelled without random jumps of cathode or anode, i.e. it referred to motion leaving continuous tracks on the cathode called 'regular movement'.

When the magnetic deflection of the a.c. arcs was opposed by wind, it was found that, at lower current and electrode spacings, the wind velocity which counterbalanced the effect of the magnetic forces and caused the arc to remain nearly stationary was approximately equal to the arc velocity in still air. As current and electrode spacing increased, the wind velocity for equilibrium fell below the still-air arc velocity.

It was observed that the cathode root could advance against the wind at about the same velocity as in still air. This observation was explained by later work which showed that the mechanism of regular movement of cathode and anode roots is situated within the electrode surface.[4]

In order to obtain more definite information on arc movement

The paper is based on Report Ref. O/T24 of the British Electrical and Allied Industries Research Association.

Dr. Guile is in the Electrical Engineering Department, Queen Mary College, University of London.

in a magnetic field, tests were made on d.c. arcs up to 900 amp.[1] These showed that the cathode root was the controlling factor in regular movement which forms a large part of the overall arc movement in the field due to current flowing in the electrodes. The cathode-root velocity was much increased if the surface of the electrode was roughened by previous arcing, rising to at least twelve times the value on a clean surface. A few tests on copper, aluminium and brass showed that their cathode velocities were similar and were lower than those on mild steel.[1]

Subsequent work in an independent investigation has shown that if the magnetic field is due to current flowing in the electrodes, cathode-root velocities on different non-ferrous electrodes vary widely but are lower than those on ferrous electrodes.[5,6] However, if an external transverse magnetic field is applied, the velocity on non-ferrous electrodes exceeds that on ferrous electrodes. This difference has been explained by the fact that it is the magnetic field within the cathode spot region which is decisive.[7] At arc currents above about 40 amp the cathode velocity is independent of current and is proportional to magnetic field strength, but it is always very sensitive to changes in the cathode surface condition.

Recent work[8] has done much to clarify the apparently conflicting results obtained by many investigators concerning the very complex processes involved in cathode-root progression, but it is not possible to give a precise mechanism at present since the processes of arc cathode emission are still uncertain. It appears likely that the motion is the net movement of individual emitting sites, each site carrying about the same current, and moving by numerous step processes, each of which is preceded by ion-bombardment conditioning ahead of the existing emission site. When the cathode track is discontinuous (this was at first termed random motion[1]) the motion is consistent and independent of arc current, but the arc column also then plays a role.

This work has demonstrated the vital role of cathode surface conditions in arc movement, and has shown that the concept often employed of magnetically deflected arcs behaving like physical conductors in a magnetic field is quite fallacious.

In the case of insulator protection where the arc must be moved rapidly along protective fittings to a place where no damage occurs, this movement relies on the magnetic field of the current flowing in the fittings towards the roots. For this reason the fittings to be described in the next Section were designed to give a single direction of current flow in the fittings towards the roots, so as to produce a definite driving field. The material of the fittings was found to have little effect on the time taken for the arc to travel around the deflector rings, and the times were so small (generally less than 0·1 sec) that no damage was caused while the arc was moving round the fittings. The choice of fitting material can therefore be made on other grounds, such as resistance to corrosion.

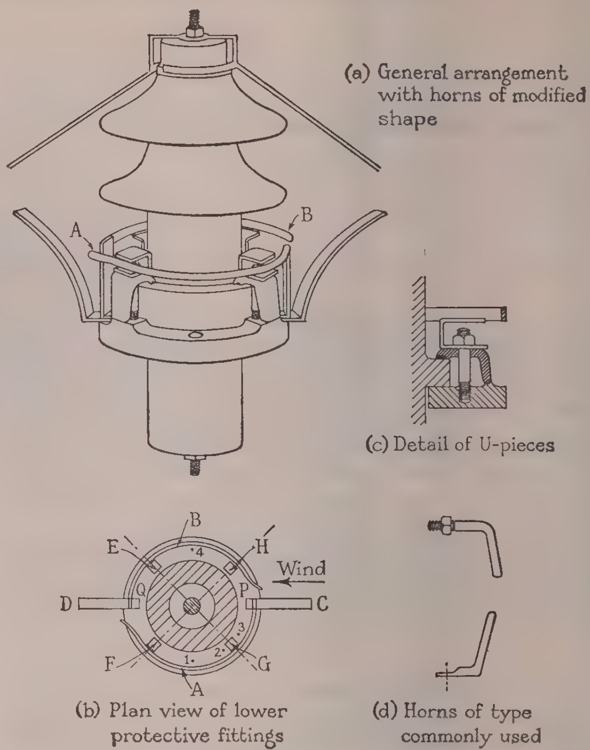

Fig. 1.—Type A 11 kV bushing and protective fittings.

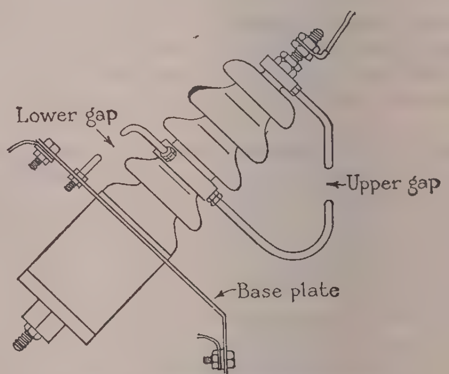

Fig. 2.—Type B 11 kV bushing.

(3) BUSHINGS

Two types of bushing were tested: Type A were 11 kV and 33 kV bushings with a single pair of arcing horns. Since the start of this investigation, this type of bushing has been superseded in Great Britain by type B, but is still widely used abroad where the 11 kV bushing shown in Fig. 1 frequently has no arcing horns. Type B were 11 kV and 33 kV bushings with two gaps in series which conform to British Electricity Specification T1 1954 for distribution transformers. The 11 kV bushing is shown in Fig. 2 in the inclined position in which it is normally mounted on a transformer tank.

A power-follow-through arc may arise on a bushing or other insulator, either following lightning flashover, when it is between the arcing horns or nearest metal parts, or it may be across the surface following flashover due to deposits, so that both conditions were tested.

11 kV bushings of type A, with a single pair of arcing horns of the usual kind shown in Fig. 1 (*d*) with a gap of 1·25 in situated 1·25 in from the sheds, were tested with a wind blowing an arc between the horns directly towards the sheds. It was found that, for a given arc current, as wind velocity increased there were four stages in the position of the arc:

(*a*) When the arc roots were on the horn tips and the column was clear of the sheds.

(*b*) When the arc roots were on the horn tips but the column was blown back on to the sheds.

(*c*) When the arc transferred to the down-wind side of the bushing.

(*d*) When the arc was quickly extinguished; this was only found to happen at low currents and high wind speeds.

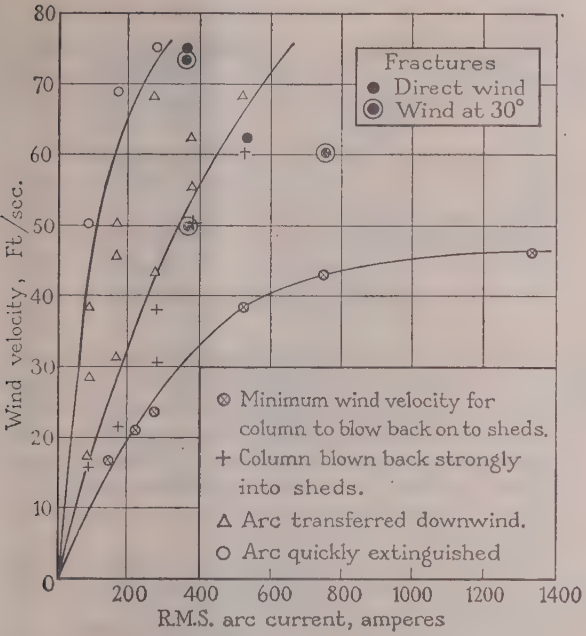

Fig. 3.—Arc initiated on horn tips with direct wind.
1¼in gap, 1¼in from sheds.

Fig. 3 shows that the boundaries of these regions are well defined. Bushings were fractured by the arc in both positions (*b*) and (*c*), and Fig. 3 shows that the currents for these fractures were quite moderate. In one case the upper shed was broken and in another the lower shed broke, due to the lower arc root remaining on a clamp bolthead near the shed. Similar fractures occurred when the wind was blowing within an angle of 60° on either side of the horns.

The type A 33 kV bushing tested with a single 10·5 in gap distant 3·5 in from the sheds was not damaged by arcs between the horns blown towards the sheds.

The type B 11 kV bushing did not suffer any damage when mounted in an inclined position with wind blowing towards an arc on the top gap, but if it were mounted vertically it would be much more likely to be damaged in the same way as type A. The type B bushing was found to be damaged in the same conditions as type A when wind was blowing towards the bottom gap, which was only 1·25 in from the sheds.

The type B 33 kV bushing had the bottom horns distant 3·5 in from the sheds so that no damage occurred when wind blew arcs on either this or the top gap towards the sheds.

When arcs were initiated over the surface of type A 11 kV bushings it was found that fracture could occur in a wind, either by the arc reaching the horns but being blown towards the sheds, or by it being blown around the bushing to the down-wind side. A new bushing was fractured in the first test at only 230 amp and 20 m.p.h. wind in this latter condition. Surface flashover in still air at some points around the bushing could cause an arc which never reached the horns but remained rooted on projections on the flange.

The type B 11 kV bushing was damaged by surface flashover arcs even in still air due to the arc remaining rooted on the clamp on the central collar. The type B 33 kV bushing was similarly badly damaged by surface flashover arcs even in still air, since the roots anchored on clamps on the central collar, on a bolt on the baseplate or on the top cap, and three different sheds were broken in various tests.

The tests on the four different bushings under the conditions of arcs between horns following lightning or across the surface thus showed the following common conditions where damage to the sheds occurred:

(i) When the wind blew the arc column back on to the sheds but was not strong enough to cause the roots to leave the tips of the arcing horns.

(ii) When the wind was strong enough to cause the arc to transfer to the down-wind side of the bushing, following either lightning or surface flashover, and—

For type A the arc was between the top cap and flange or a projection from it.

For type B the arc was between the baseplate and a clamp on the central collar.

(iii) When the arc was across the surface in still air and instead of moving to the arcing horns remained anchored on bolts or clamps on the base, on clamps on the central collar or on the top cap.

Condition (i) was remedied by finding the minimum safe distances of horn tips from the nearest sheds, and these distances are shown in Table 1 for various gaps between horns.

Table 1

Minimum Safe Distances of Horn Tips from Sheds

Gap spacing	Minimum safe distance of horn tips from sheds
in	in
1·25	2·5
2	3·5
3	4·5
4	4·5
5	4·5

Conditions (ii) and (iii) were remedied for the type A bushing by fitting a second pair of horns opposite to and in parallel with the first pair, connected to deflector rings at the top and bottom of the bushing as shown in Fig. 1. Horns of the modified shape shown in Fig. 1(*a*) were found to have some advantage over those of the usual shape [Fig. 1 (*d*)], as the burning of the horns did not increase the gap as much and the arc roots were kept further from the sheds. The U-pieces shown in Fig. 1 (*c*) are an extra but not essential safeguard, provided that the deflector rings are close to the bolts.

For type B bushings, conditions (ii) and (iii) were remedied by fitting deflector rings at the top, bottom and centre of the bushing, with projections on the central collar eliminated and U-pieces fitted to the roots of the horns as shown in Fig. 4.

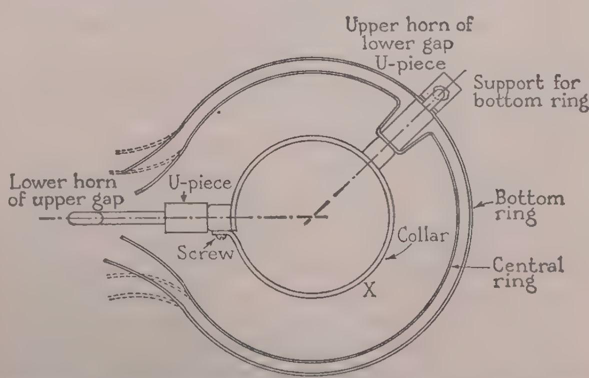

Fig. 4.—Deflector ring at centre and base for type B bushings.

The use of these modified fittings would appear to offer a considerable increase in protection, e.g. type A 11 kV bushings suffered 15 cases of complete fracture of the sheds in 250 tests, but in a similar number of even more severe tests with modified fittings not a single fracture occurred.

(4) LINE INSULATORS

This Section deals with tests made on various types of porcelain and toughened-glass 11 kV and 33 kV line insulators. For the insulators which are often used with arcing fittings, tests were made on typical fittings.

Insulators which conform to B.S. 137: 1941 have a rating number which is the voltage (expressed in kilovolts) applied in the 1 min rain test.

(4.1) Pin Insulators

Tests were made on three toughened-glass pin insulators of B.S. ratings 50, 70 and 90 (types A, B and C of Fig. 5) and on a

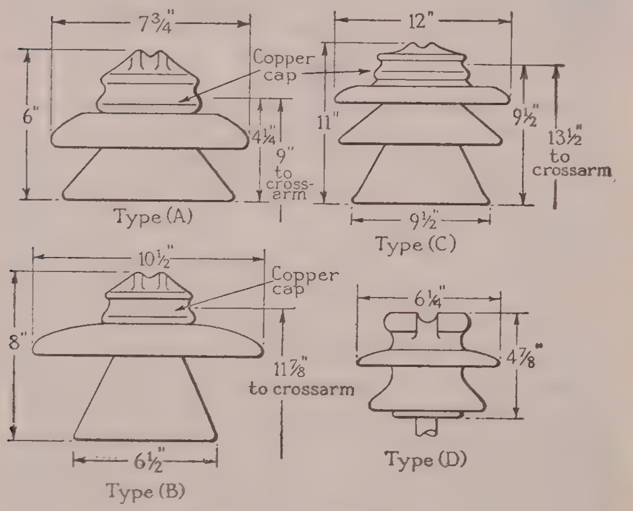

Fig. 5.—Pin insulators.

porcelain insulator of B.S. rating 50 (type D). Both porcelain and glass insulators were tested with a conductor secured with binding wire to the porcelain head in the case of the former and to the copper cap in the case of the latter, and the insulators were mounted at the end of a metal cross-arm.

The only condition in which fracture occurred to the type A and B insulators was when a wind was blowing at speeds of 20 m.p.h. or more at right angles to the conductor. This caused the arc to remain on the down-stream side of the insulator and the top shed broke. The type C insulator was not fractured in similar tests, probably because the copper cap was further from the top shed than for the other insulators.

The type D insulator was tested with stirrups bent down to form arcing horns. These horns were made to the shape given in B.S. 1320: 1946 and shown in Fig. 6 (*a*). The bottom shed of the insulator was fractured in the first test owing to the horn directing the top root towards the bottom shed. In all further tests the stirrups used were of the shape shown in Fig. 6 (*b*) and this avoided similar damage.

The bottom shed of the insulator was shattered when an arc took place across the surface at right angles to the conductor although there was no wind. This was caused by the bottom root remaining anchored high up on the pin under the sheds.

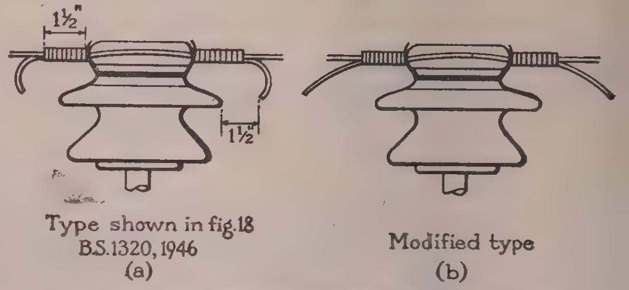

Fig. 6.—Type D pin insulator fitted with horn stirrups.

In the case of the glass insulators, burn marks were found near the top of the pins but the glass did not break, perhaps partly because for these insulators the bottom shed was further from the pin than in the case of the porcelain insulator. Fracture of the pin insulators due to the arc root on the pin happened many times in still air, and it would seem that unless the shape of insulators were altered, the only remedy would be to have four projecting horns fitted to the pin.

The porcelain insulator would be liable to damage to the top shed in a similar way to the glass insulators if an arc were blown at right angles to the conductor.

If any protective fittings were economically justified on these glass and porcelain insulators (which Section 6.1.1 shows would only be likely in special cases), they could take the form of two horns one at each side of the top cap at right angles to the conductor. The particular porcelain insulator tested would also need four short projections from the pin if complete protection were sought, but it should be noted that large numbers of this type of pin insulator are in service with a good record.

(4.2) Post Insulators

An 11 kV pedestal insulator (type E in Fig. 7) carrying a conductor and mounted on a cross-arm was tested with arcs across the surface in still air, and with wind blowing at right angles to the conductor. In contrast to results of similar tests on pin insulators, no damage occurred to this pedestal insulator.

A solid-core 11 kV post insulator (type F in Fig. 7), carrying a conductor and at the end of a metal cross-arm, suffered damage to the bottom shed when a wind of only 13 m.p.h. was blowing at right angles to the conductor. The insulator was at the down-stream end of the cross-arm so that the bottom root on the end of the cross-arm remained near the bottom shed, and wind in the opposite direction would probably not have caused damage.

A solid-core 33 kV post insulator (type G in Fig. 7) suffered burning of the binding wire which might have released the conductor when arcs were on the side at right angles to the conductor, both in still air and in a wind. In the latter case the lower rim of the head was broken as well. It was found that using four horns at the top of the insulator, two to prevent fracture and burning of the binding wire and two to prevent burning of the conductor, gave satisfactory protection.

The damage to the lower shed of the 11 kV insulator and to the head of the 33 kV insulator was not sufficient to affect their operation, so that damage to binding wire or conductor might be of greater significance.

(4.3) Suspension Insulators

The porcelain insulators were tested in three unit strings so that the effect of arc roots at the ends of the string and of cascading arcs could be studied. A conductor was held in the

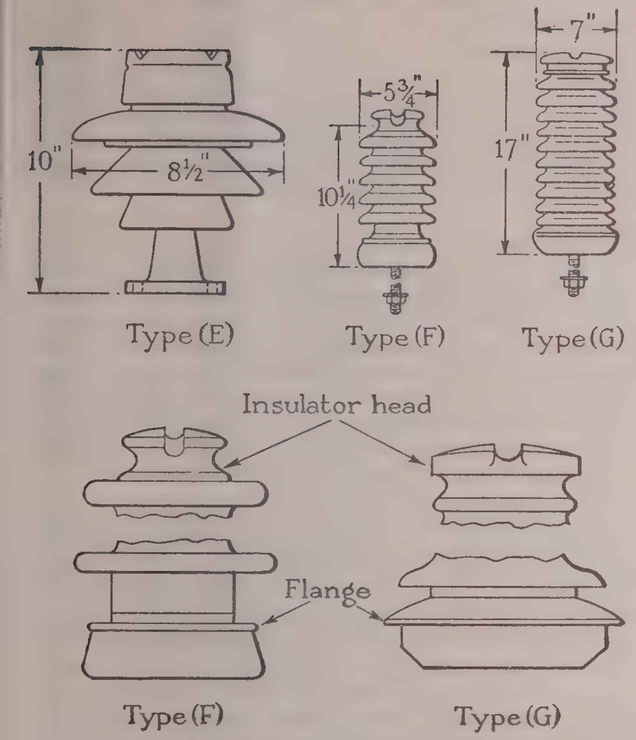

Fig. 7.—Post insulators.

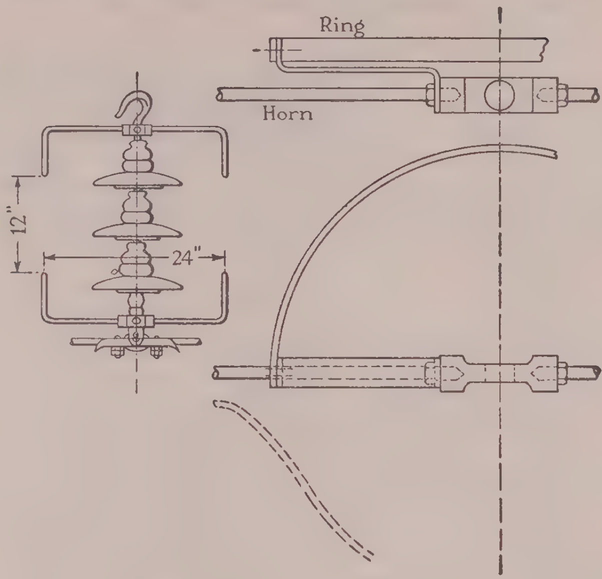

Fig. 8.—Suspension insulators.

bottom clamp and was fed with current at one end only or from both ends (ring-main connection).

In still air it was found that when the arc was initiated on the same side of the insulator as the current feed, by means of fuse wire between the top cap and bottom pin simulating lightning flashover, the bottom arc root sometimes moved through 90° and then remained anchored on the clamp pin, thus fracturing the corrugations under the bottom shed. When the arc was initiated at this 90° position the bottom root remained anchored on the pin and broke the corrugations.

When current was fed from both ends of the conductor to an arc initiated across the surface, the bottom shed was fractured on more than one occasion in still air. When wind blew parallel to the conductor the bottom shed was shattered by arcs blown to the down-stream side.

The arc root on the top cap of the upper unit was found to be directed downwards from the rim towards the porcelain by magnetic forces, causing considerable burning of the shed, but after about 0·3 sec thermal forces caused the root to ascend. In this time the available current of 900 amp was not sufficient to break the shed.

The arc roots left the metal fittings above and below the middle unit in times less than 25 millisec so that cascading did not cause any damage.

When double-point arcing horns parallel to the conductor were fitted at both ends of the string, as shown in Fig. 8, it was found that damage could still occur to the bottom shed when wind was blowing at right angles to the conductor, as the arc remained rooted on the pin. It was found that use of a deflector ring shown in Fig. 8 avoided this damage.

These tests thus showed that, for a 3-unit string without protective fittings, either line or earth end units may be damaged, with more chance of the former, and it would appear that these results would apply to longer strings. This damage can occur in still air and is even more likely in a wind. Double-point arcing horns at the bottom of the string, while protecting the conductor, do not necessarily prevent damage to the bottom insulator, unless used in conjunction with the deflector rings.

(5) ARC CURRENT AND DURATION

(5.1) Effect on Damage to Insulators

In order to gain some knowledge of the time for which arcs of a given current can burn against an insulator without fracturing it, tests were made on a toughened-glass disc in the gap between two electrodes.

Fig. 9 shows for three spacings between the electrodes corresponding values of arc current and the minimum arc duration for fracture of the glass disc.

It would appear from these results that as arc current is increased above about 1 kA, the minimum duration for fracture is falling only slowly and is in the region of 0·5 sec, and at these currents and times fracture is not affected much by whether the insulator is near the arc root or two or three inches away from it. There is, therefore, not much difference in the effect of the arc-root portion or column, provided that both parts of the arc remain close to the insulator. Tests described in the previous Sections showed, however, that damage to an actual insulator is usually caused by the part of the arc very near to the roots. This is because this part tends to remain fairly stationary under the influence of the magnetic field of the current flowing towards the arc root, which is generally on a projection and hence tends to be aligned with that projection.

(5.2) Effect on Burning of Arcing Horns

The increase in gap between ⅜ in-diameter mild-steel electrodes was measured, and Fig. 10 shows how the burning varies with current and arc duration. This curve relates to 1 in and 3 in gaps between electrodes, but it was found that there was a smaller loss of metal for 6 in and 10 in gaps, probably owing to greater movement of the roots on the electrode tips with the longer arc. In tests on bushings fitted with tinned-brass horns,[2] some measurements of burning had been made and these indicated

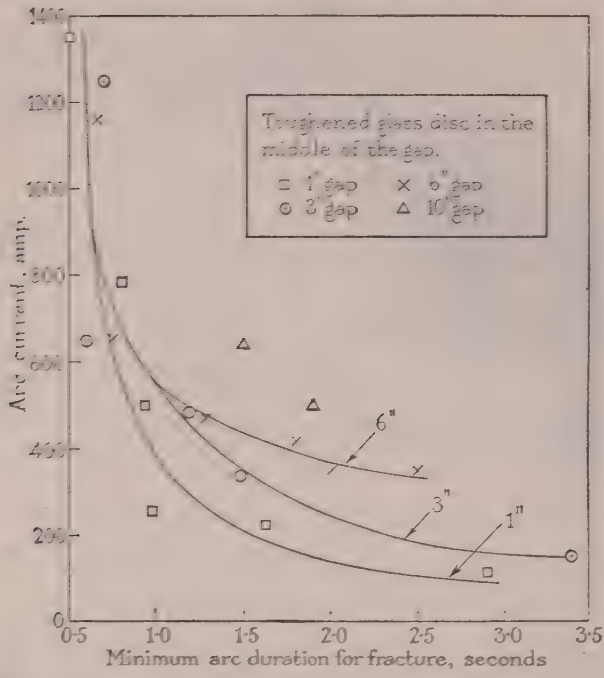

Fig. 9.—Relation between minimum arc duration for fracture and arc current.

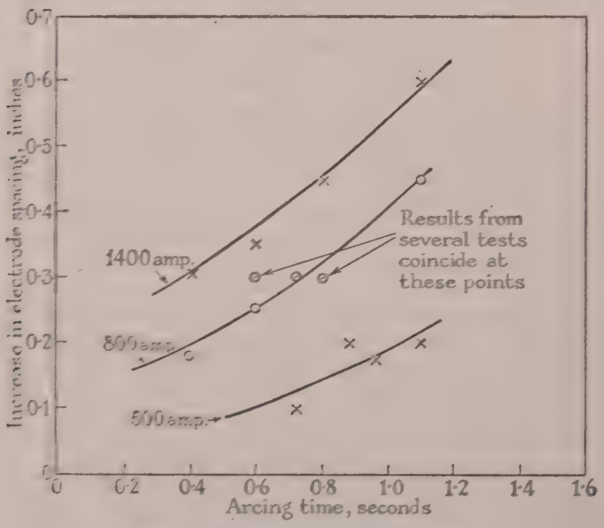

Fig. 10.—Mild-steel electrode burning.
Electrode spacings of 1 in and 3 in.

that the increase of gap for brass was of the order of double that for steel. It might therefore be an advantage to return to the use of galvanized steel for these arcing horns.

(6) SERVICE EXPERIENCE OF INSULATOR DAMAGE

(6.1) In Great Britain

The previous Sections have indicated ways in which various kinds of high-voltage insulators may suffer damage from power arcs, and have suggested methods by which greater protection can be given. From these results it would be possible to suggest fittings for other and larger insulators if greater protection for them were sought.

No action on further protection can, of course, be taken on such evidence alone, since it is not economic to provide extra fittings on each of a large number of insulators, unless without these fittings there would have been more than a certain number of cases of serious damage. This Section will therefore give some information on the numbers of insulators damaged by power arcs in this country and abroad.

(6.1.1) Up to 66 kV.

No statistics are available for damage to insulators caused by surface flashover, but a considerable amount of information exists in the case of lightning.[9]

When an insulator is damaged by a power arc following lightning, there may be either[9] (*a*) a circuit interruption with persistent damage, when supply is not restored until the damaged insulator has been replaced, or (*b*) a circuit interruption with transient damage, when an insulator has sustained slight damage which may, or may not, require later replacement, but which does not prevent a restoration of the supply.

Insulators and bushings form a large fraction of the total items of equipment damaged by lightning, but only between one in four and one in seven of all circuit interruptions are persistent. Furthermore, of the 1–$1\cdot5 \times 10^6$ insulators in service in this country, only some 600 are damaged each year by lightning, i.e. about 0·04% per annum (about 1·5 per 100 route-miles per annum). Thus the number damaged each year is small relative to the total and will probably fall further with the increased use of unearthed lines. Also, in many cases (of the order of 30–50%) a lightning incident occurs with insulator damage but without the interruption being persistent. At these voltages the actual cost of the insulator is hardly such as to make that an additional major factor.

It may therefore be concluded that, in general, it is not economic to make additions or alterations to fittings on line insulators up to 66 kV. However, the rate of damage to insulators at special positions in the line, e.g. at switchgear, transformers, T-points, etc., is somewhat higher than the overall rate. For example, of 150 cases of damage in the year 1953–54, 24, i.e. 16%, were at special positions, while of the total in this country, only about 100 000, i.e. about 7%, are at special positions. Thus the damage at special positions given by this sample is of the order of 0·1% per annum, which is about 2½ times the overall figure. Furthermore, another 31 of the 150 insulators damaged were within one or two spans of special positions. It should also be noted that rates of damage vary considerably between Area Boards, and between different localities in one Area, and may be particularly pronounced within a few miles of major supply points, where the level of fault currents is high and the setting of the relays is slow.

It may therefore be that, though in general it is uneconomic to provide extra fittings on insulators, there may be both local and special position exceptions such as busbar insulators.

The rate of damage to bushings is somewhat higher than that to insulators. Figures available from two Area Boards each indicate that it is about 0·2% per annum. Furthermore, where there has been external flashover on a transformer, it would seem that there is frequently an interruption with persistent damage; e.g. of 65 cases of external flashover on 11 kV transformers in one year (April, 1952–March, 1953), no less than 42 caused persistent damage.[9]

Failures of transformer bushings are usually more serious than those of line insulators, and bushings frequently have fittings already, so that modifications would be relatively cheaper than installation of new fittings on insulators which, at least at 11 kV,

only rarely have them at present. For systems up to 66 kV in this country, only bushings and a few locally selected line insulators, therefore, seem to present any case for improved protection.

It seems reasonable to base these conclusions on lightning failures, and the few figures available suggest that the number of insulators damaged by other causes at these voltages is only between 25 and 75% of that due to lightning.

(6.1.2) Above 66 kV.

As the system voltage rises the ratio of faults due to industrial pollution and salt storms to those caused by lightning tends to rise, and at 132 kV the ratio is about ½ (0·6 fault per 100 route-miles per annum compared with 1·1 faults per 100 route-miles per annum). Flashover in coastal areas is still a problem and would be aggravated at 380 kV.[10]

No evidence has, however, been given which would indicate that modification to existing fittings is justified, either at 132 kV or 275 kV.

(6.2) Abroad

Reports on damage received from 23 undertakings suggest that the rate of damage to bushings on systems abroad may in some cases be about 2%, i.e. ten times the rate in this country. Flashover occurs between fittings on systems up to and including 33 kV due to birds and vermin. The risk of such flashovers might be increased by the provision of deflector fittings.

(7) CONCLUSIONS

This investigation has shown the conditions in which power-arc damage may occur to 11 kV and 33 kV insulators and bushings, and simple protective fittings have been developed which give a considerable improvement in protection. From these results it would be possible to recommend improvements in fittings on insulators or bushings used at the highest voltages.

In this country it would be uneconomic to improve protection on line insulators except in certain cases such as busbar insulators. Decisions on improvement for such insulators and on bushings where the damage is more serious and more frequent are, however, best made locally since conditions vary widely.

The greater rate of damage in some countries abroad, together with more difficult conditions, presents a strong case for improvement in protection in bushings. The choice for line insulators may be between the use of units of higher rating or improved fittings in certain positions.

(8) ACKNOWLEDGMENTS

The work was carried out in the Short-Circuit Laboratory at Queen Mary College, University of London. The author wishes to thank Professor W. J. John, to whose encouragement the work is due, and Professor M. W. Humphrey Davies for the facilities provided; the Director of the Electrical Research Association for the support of the programme and for permission to publish the paper; Mr. S. F. Mehta, who carried out much of the experimental work, and others for their help and advice, including particularly Mr. L. Gosland and Dr. C. H. W. Clark.

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DISCUSSION BEFORE THE SUPPLY SECTION, 1ST FEBRUARY, BEFORE THE NORTH-EASTERN CENTRE AT NEWCASTLE UPON TYNE, 13TH FEBRUARY, AND BEFORE THE NORTH-WESTERN SUPPLY GROUP AT MANCHESTER, 28TH FEBRUARY, 1961

Mr. N. G. Simpson: Overhead-line-fault statistics as expressed fail to reveal less favourable seasonal and regional statistics, the establishment of which would place the author's work in a better perspective. The increasing importance of both domestic and commercial consumers' installations reflects less tolerance of supply interruptions of overhead-line networks, however infrequent, apart from a constant public pressure towards buried cables from aesthetic considerations. The author indicates how protective improvements can be achieved in the vulnerable sphere of outdoor insulation, the adoption of which are warranted and comparatively insignificant in cost, if only a small degree of improvement results.

The most important contribution to distribution overhead-line operational efficiency since the recent war has been unearthed construction, both at home and oversea, where the impulse insulation of wood-pole structures is utilized to increase phase-to-earth values. For example, the impulse level of unearthed 33 kV lines is of the order of 1 000–1 500 kV to earth, which prevents earth-fault flashover from birds and dross and gives a degree of improvement in lightning performance. However, such reinforcement with metal cross-arms and insulator pins does not apply between phases, and the frequency of phase-to-phase flashovers remains. Both phase and earth insulation faults are frequent enough on earth-bonded wood and steel structures. Pin-insulator punctures occur oversea, and although timber cross-arms are used in such territories, their mechanical and electrical instability is an inherent disadvantage.

This lack of balance between earth and phase-to-phase insulation values in the overall concept of insulation 'defence in depth' may be remedied by a recent trend towards the adoption of moulded-resin cross-arms and insulator pins, service trials on which are shortly to take place in one of the United Kingdom

Area Boards. The provision of an insulated pin in place of a metal one may in any case prevent the anchorage of arcs on the latter, a feature outlined in the paper. Whether cross-arms are of insulated or metallic form it is difficult to see whether the type of insulator gap protection described can profitably be used at straight-line unearthed supports, which constitute the majority in distribution networks where B.S. 1320-type lines are used. It must be appreciated that protective gaps, to be effective, reduce overall insulation values—the premium to be paid for the service they render.

At terminals and pole-type substations, however, where transformers and switchgear are generally located, supports are earthed, and incoming surge effects are localized and tend to be more severe. The author's technique may well contribute to an outage reduction at small cost. The extent of such relief can be measured only by outlining representative cases of available operational experience. In e.h.v. systems, with their greater distances and concentrations of power, the consequential effects of insulation faults are inevitably severe, despite small circuit-breaker clearance times. With higher-voltage systems the insulator length factor affects line economics to an appreciable extent and efforts will be directed to make every insulator unit as effective as possible. Protective and grading devices are then likely to be retained and the design aspects profitably explored with the technique outlined in the paper.

A new form of e.h.v. overhead-line distribution is now under way in this country, namely railway-electrification catenary systems with a substantial mileage. Again, the technique in the paper may be applied with advantage, e.g. in the design of protective fittings for section insulators liable to be severely damaged owing to arc transference from a moving pantograph.

Mr. D. F. Oakeshott: In Section 6 the author concludes from a brief statistical survey that for systems with voltages less than 66kV in Britain it is mainly on bushings that some form of arc protection is needed. It should be noted, incidentally, that at 132kV and above some effort is made to avoid arc damage, at any rate where transformers are concerned, by placing the co-ordinating gaps well away from the bushing, e.g. on an adjacent gantry. This deals, however, only with arcs initiated by lightning or other over-voltages. Also, on the high-voltage systems, tripping times are relatively very short, so that arcs will usually persist for only a few cycles and little damage results (see Fig. 9).

The 11kV bushing with two gaps in series is very widely used, and any practical suggestions for modifications to reduce arc damage should be carefully examined. In doing this, all relevant aspects of such modifications have, of course, to be considered. In Section 3 four remedial measures are suggested, of which three seem to be perfectly feasible, but the fourth modification, the use of special deflector rings, does not appear to be a practicable design for use in this country.

First, it seems to nullify the major feature of the gap and make it even more liable to flashover from birds, etc. Secondly, the need for increased phase clearance involved in their use is likely to increase expense very considerably; and finally the impulse characteristic has been shown in some tests, performed at the Central Electricity Research Laboratories, to be considerably different from that of the simple gap. The performance is not necessarily worse, but the result emphasizes the need to look into this aspect closely.

The paper serves to draw attention to a case where a very small modification should greatly lessen the risk of damage due to arcing: in some forms of 33kV switchgear the bushings are inclined outwards from the tank, and the arcing horns are fitted on the underside of the bushing, so that commonsense considerations, and indeed actual experience, indicate that through thermal effects any arcing damages the top of the bushing. Possibly there are difficulties in fitting these horns to the upper side of the bushing, but these should not be insuperable.

Mr. D. J. Miller: In Section 4, the author has carried out a test on four designs of pin insulator, three of which were armoured glass. In tests on type C, he was unable to obtain a fracture under circumstances similar to those under which fractures were obtained in types A and B. This is ascribed to the fact that the upper copper cap in type C is mounted some small distance above the top shed. It seems rather remarkable that this should produce such a large effect, but, if it is so, it points to the fact that a large part of the energy is dissipated at the arc root. Is this true? If, in fact, such a large gain in performance can be obtained by such a small modification in the shape of the insulator, it should surely be adopted as a standard for all designs.

Can any general conclusions be drawn as to the comparable behaviour of glass and porcelain pin insulators?

Mr. R. N. Buttrey: Arcing horns on the outside of circuit-breaker bushings sloping over the top of the circuit-breaker, give convection results, driving the arc on to the bushing: flashover results in breakage of the porcelain. There is thus good reason for developing a form of arcing horn for such a bushing (which is very common in circuit-breakers, i.e. with a bushing sloping outwards), and possibly this would avoid the breakage of the porcelain. The arcing horns cannot be placed on the top of the bushing, otherwise a blow-up between phases could result.

Mr. F. C. Walmsley: In the paper the bushings are set up as normally mounted on a transformer and the current is fed from an external source to the top terminal, the arc being formed to the earthed metal. Cases can occur in which the transformer is supplying the arc current and hence the current has paths along which it flows in opposite directions. Can the author comment on the proximity magnetic effects which are present?

The space available on the top of a 33kV outdoor oil circuit-breaker is restricted by phase and pole clearances together with possible mechanism coverings. While it is desirable to have the arcing horns on the upper side of the bushings, it is not always practicable for the reasons given.

Mr. A. B. Wood (*at Newcastle upon Tyne*): The author has shown that if protective fittings are to be used certain design forms are better than others. However, the mere application of any form of arc-gap fitting to an insulator immediately brings down the insulation level and therefore increases the tendency for flashover and hence damage. It is a little difficult, therefore, to compare the probable lives of similar insulation with and without arcing gaps. The incidence of damage to insulation is very low and I do not favour the application of gaps as a general rule, in view of the resulting reduction in insulation level at the higher voltages, and the too-frequent flashovers due to birds, etc., at the lower voltages.

The application of arcing fittings to high-voltage transmission-line insulator strings is a compromise between obtaining maximum insulation level and maximum protection from cascading over the long strings. Other duties which the arcing fittings have to perform are protection of the conductor and the shielding of the suspension clamp and lower discs to prevent excessive corona and radio interference; it may even be required to provide anti-bird perches to prevent severe reduction in flashover voltages due to the habits of certain large birds.

The comment that faults due to industrial and saline pollution become more important as voltages rise presumably applies to this country because of its industrial concentration, long coast-line and low isoceraunic level. The comment is by no means true of other countries; in a recent world survey of high-voltage

transmission-line faults it was shown that about 62% of all insulator faults were due to lightning and only 22% due to pollution, the greater proportion of these latter being due to fog.

It is always possible to design a set of fittings which are sufficiently spread to keep the arcs substantially clear of the insulator string, but such fittings would almost certainly adversely affect tower costs, which are important in high-voltage transmission. A mitigating factor on high-voltage transmission is the higher-speed protection, which will tend to limt the amount of insulator damage due to power follow current. In Section 6.12, the author states that there is no evidence to indicate that modification of fittings at 132 or 275 kV is warranted. However, the Central Electricity Generating Board has recently specified that the long insulator strings which will be used at 275 kV and higher voltages shall have earth-end arcing horns in addition to live-end horns. Since this could only bring down the insulation level of the line, one must assume that the fittings are for protection of the top few discs from the effects of cascading. Any evidence of damage due to this effect would be interesting.

Mr. G. K. Simpson (*at Newcastle upon Tyne*): The category (*a*)—arcs following flashover due to lightning—should be subdivided to distinguish dry and wet insulation. Although the flashover voltage may not be affected greatly, the distance of the horn gap from the insulator to prevent the arc being initiated partly on the insulator surface is greater when the insulator is wet.

Several references are made to the fuse-initiated surface arcs transferring to the down-wind side of the insulator. There is much evidence that in practice surface arcs generally are initiated on the leeward side, owing to the wind carrying the heat and ionization with it and also to rain washing the windward side relatively clean. The wind does not have to be very strong to achieve this, and when flashover occurs much of the arc may not be in contact with the insulator at all but may loop out in air following previous surge current paths. It may be, therefore, that this particular test condition is unnecessarily severe.

How was the fuse wire for the initiating of surface arcs arranged and what are the author's views on the effect of the metal and metal vapour on the behaviour of the arc?

There is a category of damage which is peculiar to substation insulators, which is due to pollution causing mainly surface flashover resulting in an earth-fault current near to the maximum short-circuit level of the system. This current is usually cleared very rapidly by the high-speed unit protection (in a few cycles) with a minimum of damage. Nevertheless, a large insulator has been damaged usually only on the upper surface of the top shed and the damage may be confined to a few square inches of glazed surface, but because it may be performing an arduous mechanical function or it is considered that the damage may impair its electrical performance, it is scrapped.

Fig. A shows that the normal clamped or cemented terminal fitting encourages the arc to stay close to the porcelain surface. The alternative would be to use a fitting which blows off the arc electromagnetically in the shortest possible time, as in Fig. B.

Fig. A.—Insulation with cemented-on flange.

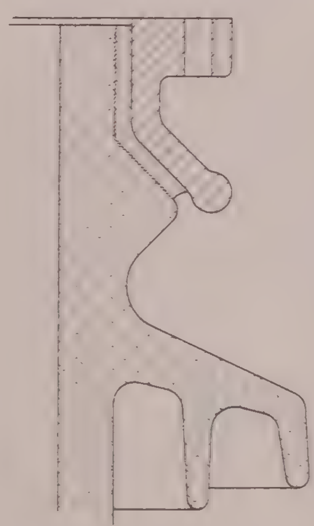

Fig. B.—Flange designed to blow-off a surface-initiated arc.

Mr. F. Mather (*at Manchester*): The author's proposals for improved and more costly arcing fittings must be viewed in relation to fault statistics, and it is of interest to note that on one system comprising 4000 miles of 33 and 11 kV overhead lines there are about 1200 interruptions in a typical year. Of these, approximately 900 are due to lightning and, in 834 of these cases, no visible damage has resulted.

Fig. 9 appears to indicate that if the product of arc current and time can be kept down to 600 amp-sec, no damage to insulators will result. In most cases this should not be difficult to achieve.

The author suggests that the number of damaged insulators should fall with increasing use of unearthed line construction. It should be borne in mind, however, that there are many points on such lines where orthodox earthing is employed.

Presumably the damage rate of 0·2% for bushings, as compared with 0·04% for insulators, is due to the fact that the bushings are concentrated at earthed positions.

Is the 2% failure rate experienced abroad, as compared with 0·2% in this country, largely due to the use of arc-suppression coils?

It is unfortunate that it was necessary to employ fuse wire to initiate arcs, since valuable information might have been gained had it been possible to simulate more closely a lightning impulse superimposed on the power-frequency voltage wave.

THE AUTHOR'S REPLY TO THE ABOVE DISCUSSIONS

Dr. A. E. Guile (*in reply*): I agree with Mr. N. G. Simpson that statistics which show a yearly rate of damage for a wide area mask seasonal and regional rates which may be very much higher. There are extreme cases, both in this country and abroad, where between 20 and 45% of the total number of faults reported in one year occurred during a single day. An example of the need for care in assessing the value of statistics occurs in the total number of cases of damage to 66 kV insulators on individually earthed poles, reported from ten Area Boards during a one year period: all fifteen insulators were damaged as a result of a single lightning stroke.[9] Mr. Simpson's remarks on the recent development of moulded-resin cross-arms and insulator pins emphasize the suggestion in the paper that the use of modified fittings would be confined to insulators at points where supports are earthed. Mr. Simpson's suggestion that the technique may be applied with advantage to the railway overhead systems is very timely, since persistent breakage of insulators due to power flashovers has occurred at two places on the newly electrified system during the past winter. Fittings based upon those described in the paper have recently been tested up to 6 kA, and since they have proved satisfactory, they are now being fitted to large numbers of insulators at these points. The question of section insulators suggested by Mr. Simpson is now coming under consideration.

Although, as Mr. Oakeshott and Mr. Wood remark, the arcs on systems of 132 kV and above will usually persist for a few cycles only, circumstances could arise where arcing times of about 1 sec might occur, e.g. if busbar protection were out of action. Even with the much shorter arcing times which occur in the majority of faults, the glaze, particularly on the earth-end unit, may be damaged. It is for this reason (and also to give a definite controlled gap) that the Central Electricity Generating Board now specifies that insulator strings at 275 kV and above must have earth-end arcing horns, as mentioned by Mr. Wood.

As Mr. Oakeshott says, the addition of deflector rings would have disadvantages, and these are part of the evidence which must be weighed by an engineer who is deciding whether or not to use these fittings. There may, for example, be a compromise between the damage likely to be avoided by their use and that which may be caused by birds. The top and bottom rings on the bushing should not greatly affect the latter, and it may be possible, perhaps by making the diameter of the central ring less than that of the others, to reduce outages due to birds.

Mr. Buttrey, Mr. Walmsley and Mr. Oakeshott refer to the difficulty of placing horns on switchgear bushings which slope outwards, owing to the need to avoid damage due to the arc rising thermally if the horns are below the bushings, and interphase flashover if they are above. Each switch would have to be treated on its own merits to find the best position, but as Mr. Oakeshott indicates, a reasonable solution should be possible. Tests at the full fault current might be needed in order to be certain that any particular arrangement would be satisfactory.

In reply to Mr. Miller, it is true that a large part of the arc energy is dissipated at the arc roots, and I agree with his remark that a small modification in the shape of an insulator, and also in the shape of the metal fittings adjacent to the insulator surface, may make a considerable improvement in performance. Mr. G. K. Simpson has shown a possible modification in Fig. B and I agree with this. I hope that modifications of this kind will be put to the test. No attempt could be made from the tests described to compare glass and porcelain pin insulators as Mr. Miller asks. Such a comparison would be invalidated, for instance, by the differences in the position of arc roots relative to the top insulator shed.

In reply to Mr. Walmsley, the current flowing inside a bushing when a transformer acts as the source will not affect the arc for the following reasons. The arc movement is controlled by conditions at the cathode root,[4–7] and it is the magnetic field in the cathode-fall region (within $\sim 10^{-3}$ cm from the surface at atmospheric pressure) which is critical.* Experiments now in progress show that this magnetic field is due mainly to the majority of the current in the electrode flowing within a fraction of a millimetre from the surface near the cathode root, owing to skin effect caused by the high rate of change of magnetic field set up by the movement of emitting sites. Thus roots on the flange or top cap would not be affected by the current flowing in the relatively distant conductor in the bushing.

Mr. G. K. Simpson asks how the fuse wire was arranged to simulate surface arcs, and he and Mr. Mather ask what effect the wire had on the subsequent arc. The wire was held against the sheds by cotton. It was originally suggested that an alternative method of initiation should be sought, but many films at 960 frames/sec showed that, after one or two milliseconds, the arc behaviour was apparently unaffected by the metal vapour, and we continued to use fuse wire for all our work.

The use of arc-suppression coils does not seem to be a factor, as suggested by Mr. Mather, in the difference between the bushing failure rates in this country and in some territories abroad, since they are little used in either case. One major factor is the frequency of lighting: the number of thunderstorm-days each year in one of the countries concerned is several times higher than the average in this country. In the case of the other undertaking with a high failure rate, none of the bushings damaged was protected by rod gaps, whereas in this country they would have had protective fittings.

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THE MATHEMATICAL BASIS OF THE ABSOLUTE CALIBRATION OF VOLTAGE DIVIDERS FOR THE MEASUREMENT OF FRONT-CHOPPED IMPULSE WAVES

By M. OUYANG, B.Eng., Ph.D., Associate Member.

(*The paper was first received* 19*th December*, 1960, *and in revised form* 14*th April*, 1961.)

SUMMARY

A new technique has recently been developed in the absolute calibration of dividers for the measurement of an impulse voltage which is chopped at its front within a fraction of a microsecond. By this technique the theoretical error of a divider under idealized conditions can be calculated from the 'ultimate time-displacement', which is derived from the response of the divider to a unit-function voltage.

The paper discusses theoretically the basis and limitations of this technique and derives some properties of the ultimate time-displacement which are of practical significance.

LIST OF PRINCIPAL SYMBOLS

$\bar{g}(p)$ = Laplace transform of any time function $g(t)$; i.e.

$$\bar{g}(p) = \int_0^\infty \varepsilon^{-pt} g(t)dt.$$

$v_i(t)$ = Input to a divider.

$v_o(t)$ = Output of a divider.

$\mathrm{f}(p)$ = Operational coefficient of the equation $\mathrm{f}(p)v_i(t) = v_o(t)$; hence $\mathrm{f}(p) = \bar{v}_o(p)/\bar{v}_i(p)$.

$\mathrm{f}(0)$ = Steady-state divider ratio at very-low frequencies = $\lim_{p \to 0} \mathrm{f}(p)$.

$v_{on}(t)$ = Normalized output = $v_o(t)/\mathrm{f}(0)$.

$\mathrm{H}(t)$ = Heaviside unit function.

$\mathrm{h}(t)$ = Indicial response, i.e. output of a divider for an input $\mathrm{H}(t)$.

$\mathrm{h}_n(t)$ = Normalized indicial response = $\mathrm{h}(t)/\mathrm{f}(0)$.

$\mathrm{T}(t)$ = Time-displacement function of a divider

$$= \int_0^t [\mathrm{H}(t) - \mathrm{h}_n(t)]dt.$$

T = Ultimate time-displacement of a divider

$$= \int_0^\infty [\mathrm{H}(t) - \mathrm{h}_n(t)]dt.$$

t_u = Minimum value of t at which the following approximation is permissible: $\mathrm{T}(t) \simeq T$.

t_o = Chopping time, i.e. the time interval between the start of $v_i(t)$ and the instant of chopping.

Throughout the paper, parentheses in mathematical expressions are used exclusively to enclose the variable of a function.

(1) INTRODUCTION

The divider for the measurement of high impulse voltages has been extensively studied. The technique has advanced to such an extent that there is no insurmountable difficulty in the measurement of impulse voltages of the standard waveform ($1/50\,\mu$s in Europe and $1{\cdot}5/40\,\mu$s in the United States) to an accuracy within 3–5%, provided that the wave is not chopped at its front. Furthermore, for such measurements, the performance of a divider can always be checked with a standard sphere-gap. No such standard device, however, exists for the measurement of voltage waves chopped at the front. The accurate measurement of such impulse waves is required in many operations such as the testing of surge diverters and the study of impulse puncture of insulators. In theory it is possible to calibrate a divider for these measurements according to classical analysis,[1] but in practice the calibration cannot be carried out conveniently. In recent years a new technique has been developed (Hylten-Cavallius,[2] Özkaya *et al.*[3] and Creed[4]) which provides a method for the absolute calibration of a divider. This technique is to measure the response of the divider to a unit-function input (called the indicial response) and to derive from it two parameters, the 'time-displacement function' and the 'ultimate time-displacement', which will be defined later. The front-chopped input wave and the corresponding output wave can then be correlated in terms of these parameters. There is reason to believe that this technique will be adopted by national and international authorities in forming standard specifications for impulse measurements.

As well as discussing theoretically the basis and limitations of this new technique, the paper includes the derivation of some important properties of the ultimate time-displacement which are of practical significance. It is shown that the ultimate time-displacement can be derived from the response to waves of shapes other than unit-function. It is also shown that the shortest chopping time which can be measured with a required accuracy depends not only on the characteristics of the divider but also on the method of chopping.

(2) CHARACTERISTIC PARAMETERS OF A DIVIDER

The basic concepts, which were first introduced by Hylten-Cavallius,[2] are now discussed. The divider is here considered to include the entire measuring system, from the points across which the voltage is to be measured to the deflecting plates of the cathode-ray oscillograph. It is also considered to have linear characteristics and to be initially at rest.

To simplify the mathematical expressions, the output wave, $v_o(t)$, is normalized by multiplying its amplitude by $1/\mathrm{f}(0)$, where $\mathrm{f}(0) = \lim_{p\to 0} \mathrm{f}(p)$ and $\mathrm{f}(p) = \bar{v}_o(p)/\bar{v}_i(p)$. Furthermore, the output wave is deemed to start at the same instant as the input wave. This shift of the time origin of the output wave does not invalidate the derived conclusions.

Let $\mathrm{h}_n(t)$ be the normalized indicial response. Consider a hypothetical linear input $v = \alpha t$, α being a constant. The normalized output is shown by Duhamel's theorem to be

$$v_{on}(t) = \alpha \int_0^t \mathrm{h}_n(t)dt$$

Define $\mathrm{T}(t)$ as $\int_0^t [\mathrm{H}(t) - \mathrm{h}_n(t)]dt$. The above equation can then be written

$$v_{on}(t) = \alpha t - \alpha \mathrm{T}(t) = \alpha[t - \mathrm{T}(t)] = v_i(t - \mathrm{T}(t)) \quad . \quad (1)$$

Written contributions on papers published without being read at meetings are invited for consideration with a view to publication.

The paper is based on Report Ref. S/T100 of the British Electrical and Allied Industries Research Association.

For a practical divider for impulse measurement, $\mathrm{T}(t)$ approaches a constant T in time (see Section 7). For a required accuracy, the following approximate is permissible:

$$\mathrm{T}(t) \simeq T, \text{ for } t \geqslant t_u$$

Therefore
$$v_{on}(t) \simeq v_i(t - T), \text{ for } t \geqslant t_u \quad . \quad . \quad . \quad (2)$$

It is seen from eqns. (1) and (2) that the output for a linear input is displaced along the time axis from the latter by an amount $\mathrm{T}(t)$ at any instant t, and ultimately by an amount T. For this reason, we have called $\mathrm{T}(t)$ and T the 'time-displacement function' and the 'ultimate time-displacement' respectively.

According to their definitions, $\mathrm{T}(t)$ and T of a divider can be calculated from the measured indicial response. As a first approximation, a front-chopped wave can be regarded as having linear front and tail so that it can be expressed as the sum of

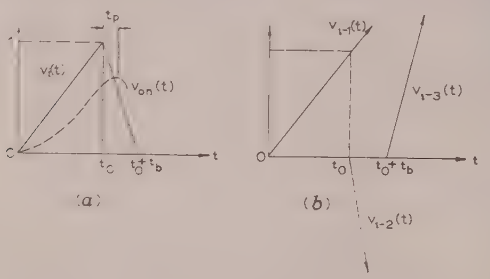

Fig. 1.—Components of a front-chopped wave.
(a) Input and output waves.
(b) Components of input wave.

three linear component waves (see Section 4 and Fig. 1). Thus its output wave can be expressed in terms of these component waves and $\mathrm{T}(t)$ or T according to eqn. (1) or eqn. (2).

(3) SOME PROPERTIES OF ULTIMATE TIME-DISPLACEMENT

The following properties of ultimate time-displacement are derived in Section 7:

(a) For any input voltage which approaches a constant non-zero value, k, the integrated area, with respect to time, which lies below it and above the normalized output voltage, is equal to k times the ultimate time-displacement.

(b) For any input voltage, $v_i(t)$, which approaches a constant non-zero value, k, while the integrated area, A_1, above $v_i(t)/k$ and below the unit function is finite, the corresponding area, A_2, below the unit function and above $v_{on}(t)/k$ is also finite. The ultimate time-displacement is equal to $A_2 - A_1$.

(c) When a divider consists of a chain of cascaded elements without mutual coupling, and when each element introduces negligible loading to its preceding elements, the ultimate time-displacement of the whole divider equals the algebraic sum of the ultimate time-displacements of all the elements.

According to its definition, the ultimate time-displacement is to be determined from the indicial response. Creed[4] has shown that, when a sphere-gap in compressed nitrogen (300 lb/in^2) breaks down, the collapse of voltage is fast enough to be considered essentially as a unit-function wave. The ultimate time-displacement of a divider can thus be determined at its working voltage level by applying an impulse wave to the divider in parallel with the pressurized gap and by adjusting the gap to break down at or near the peak of the wave. When high precision is required it is important that the peak of the wave should be sufficiently flat so that the divider output has an essentially constant value over a period just prior to the breakdown of the gap. When the voltage collapse is not a true unit-function wave, the derived ultimate time-displacement is larger than its true value. With a knowledge of the time taken for the voltage to collapse completely, the error can be estimated using property (b).

An acceptable approximate unit-function wave can also be produced at a few hundred volts in a similar manner by substituting a mercury-wetted contact[2] for the pressurized sphere-gap. Owing to the low value of divider ratio, the output in this case can only be measured through an amplifier. As, according to property (c), the measured value of the ultimate time-displacement includes that due to the amplifier, it may be appropriately corrected provided that the amplifier introduces negligible loading to the divider.

If calibration is to be carried out at this low voltage level, the input wave can be measured directly. Property (a) shows that the calibration can then be carried out by any input wave, provided it meets the stated requirements.

(4) LIMITATION IMPOSED ON A DIVIDER BY THE METHOD OF CHOPPING

A linear wave chopped at $t = t_0$ can be approximated by a linear wave, $v_i(t) = t/t_0$, in a normalized unit up to the instant $t = t_0$. The normalized output of a divider is then approximately $v_{on}(t) = [t - \mathrm{T}(t)]/t_0$ up to the instant $t = t_0$. By assuming that $v_{on}(t)$ reaches its peak at $t = t_0$, i.e. by neglecting any error in the measurement of the chopping time, the per-unit error of the peak voltage can be taken as

$$\delta_1 = \frac{v_i(t_0) - v_{on}(t_0)}{v_i(t_0)} = \frac{\mathrm{T}(t_0)}{t_0} \quad . \quad . \quad . \quad . \quad (3)$$

When $t_0 > t_u$, the per-unit error can be taken as

$$\delta_2 = \frac{T}{t_0} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (4)$$

Generally, however, $v_{on}(t)$ does not reach its peak at the instant $t = t_0$. Consider $e_i(t)$ to increase linearly to its peak at the instant $t = t_0$ and then decrease linearly to zero at $t = t_0 + t_b$ (Fig. 1). $v_i(t)$ can be resolved into three linear components:

$$\left.\begin{aligned} v_{i-1}(t) &= [t/t_0]\mathrm{H}(t) \\ v_{i-2}(t) &= -[1/t_0 + 1/t_b][t - t_0]\mathrm{H}(t - t_0) \\ v_{i-3}(t) &= \{[t - t_0 - t_b]/t_b\}\mathrm{H}(t - t_0 - t_b) \end{aligned}\right\} \quad . \quad . \quad (5)$$

Thus

$$\left.\begin{aligned} v_{on}(t) &= \frac{t - \mathrm{T}(t)}{t_0} \quad \text{for } 0 \leqslant t \leqslant t_0 \\ &= \frac{t - \mathrm{T}(t)}{t_0} - \left[\frac{1}{t_0} + \frac{1}{t_b}\right][t - t_0 - \mathrm{T}(t - t_0)] \\ &\qquad \text{for } t_0 \leqslant t \leqslant t_0 + t_b \\ &= \frac{t - \mathrm{T}(t)}{t_0} - \left[\frac{1}{t_0} + \frac{1}{t_b}\right][t - t_0 - \mathrm{T}(t - t_0)] \\ &\qquad + [t - t_0 - t_b - \mathrm{T}(t - t_0 - t_b)]/t_b \\ &\qquad \text{for } t_0 + t_b \leqslant t \end{aligned}\right\} \quad . \quad (6)$$

Eqn. (6) indicates that $v_{on}(t)$ may continue to increase after $t = t_0$ and only reaches its peak at, say, $t = t_0 + t_p$. By equating $\dot{v}_{on}(t) = 0$, it follows that t_p must satisfy

$$\frac{h_n(t_0 + t_p)}{t_0} = \left[\frac{1}{t_0} + \frac{1}{t_b}\right] h_n(t_p)$$
$$\text{for } 0 < t_p < t_b \quad . \quad . \quad (7)$$

or $$\frac{h_n(t_0 + t_p)}{t_0} = \left[\frac{1}{t_0} + \frac{1}{t_b}\right]h_n(t_p) - h_n(t_p - t_b)/t_b$$
$$\text{for } t_b < t_p \quad . \quad . \quad . \quad (8)$$

Therefore, t_p depends not only on the characteristics of the divider in terms of $h_n(t)$ but also on the value of t_b which is dictated by the method of chopping.

Consequently, by taking the time for $v_{on}(t)$ to reach its peak as the chopping time the result contains a per-unit error of

$$\delta_3 = \frac{t_0 - (t_0 + t_p)}{t_0} = -\frac{t_p}{t_0} \quad . \quad . \quad . \quad . \quad (9)$$

which, owing to the numerator being t_p, depends not only on the characteristics of the divider but also on the method of chopping.

It will be seen that $|\delta_3|$ decreases with increasing value of t_0. Therefore, when a given value of $|\delta_3|$ is not to be exceeded the divider must not be used to measure waves chopped earlier than a certain limiting time. This limit is imposed on the divider by the method of chopping used.

When the T(t) or h(t) of a divider is known, and when the error in chopping time ($|\delta_3| = t_p/t_0$) is required not to exceed a given value, the relation between the minimum permissible value of t_0/t_b and the maximum permissible value of T/t_b can be calculated from eqn. (7) or eqn. (8). As an example, Fig. 2

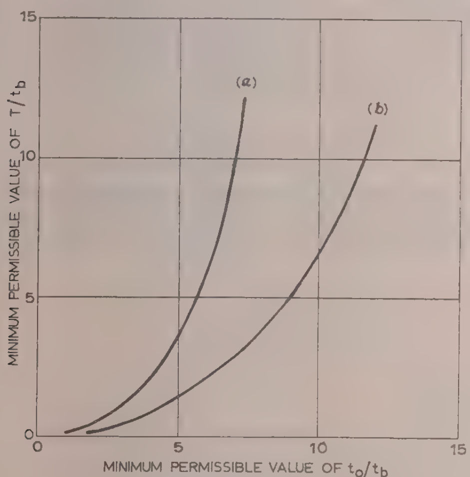

Fig. 2.—Limitation of a divider.
$T(t) = T(1 - \varepsilon^{-t/T})$
(a) $t_p/t_0 \leqslant 0{\cdot}10$.
(b) $t_p/t_0 \leqslant 0{\cdot}05$.

shows this relation for a divider having $T(t) = T[1 - \varepsilon^{-t/T}]$. If the ultimate time-displacement of the divider is $0{\cdot}1\,\mu s$ and the method of chopping gives $t_b = 0{\cdot}02\,\mu s$, i.e. $T/t_b = 5$, Fig. 2 shows that, if $|\delta_3|$ is to be limited to $0{\cdot}10$, t_0/t_b must not be smaller than $5{\cdot}7$, and consequently the divider can only be used to measure waves chopped after $5{\cdot}7 \times 0{\cdot}02 = 0{\cdot}114\,\mu s$. Similarly, if $|\delta_3|$ is not to exceed $0{\cdot}05$, the divider can only be used to measure waves chopped after $8{\cdot}9 \times 0{\cdot}02 = 0{\cdot}178\,\mu s$. Table 1 gives some further numerical examples for $t_p/t_0 \leqslant 0{\cdot}10$. For $t_p/t_0 \leqslant 0{\cdot}05$ the corresponding values of minimum allowable t_0 would be higher.

For a sphere-gap in air at atmospheric pressure and at about 50% breakdown voltage, t_b is about $0{\cdot}03\,\mu s$. At high overvoltage, t_b should be smaller, but even with $t_b = 0{\cdot}01\,\mu s$ and $T = 0{\cdot}01\,\mu s$, the divider cannot be used to measure a chopping time of less than $0{\cdot}03\,\mu s$, when t_p/t_0 is to be limited to $0{\cdot}10$ (see Table 1). If the present practice of chopping with rod-gaps is to be maintained, the use of the divider should be limited to the measurement of still larger chopping time. This is because t_b for a rod-gap is larger than that for sphere-gap.

Table 1

MINIMUM CHOPPING TIME TO LIMIT t_p/t_0 TO $0{\cdot}1$

Maximum allowable t_b	Minimum chopping time for T equal to			
	$0{\cdot}100\,\mu s$	$0{\cdot}050\,\mu s$	$0{\cdot}025\,\mu s$	$0{\cdot}010\,\mu s$
μs	μs	μs	μs	μs
0·030	0·15	0·11	0·08	0·05
0·020	0·11	0·09	0·06	0·04
0·010	0·07	0·06	0·04	0·03

(5) ACKNOWLEDGMENT

The author wishes to express his thanks to Dr. R. H. Golde for his interest and to Dr. F. C. Creed for his comments. Thanks are also due to the Director of the British Electrical and Allied Industries Research Association for permission to publish the paper.

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(7) APPENDICES

(7.1) Convergence of the Time-Displacement Function

The technique discussed in the paper can be used only for dividers of which the time-displacement function, T(t), is convergent. This, however, does not impose undue restriction on the physical nature of the divider. A practical divider for impulse measurement must, and does, satisfy the following requirements.

(*a*) Its response to a unit impulse-function input does not contain a d.c. component or any component which increases indefinitely with time.
(*b*) Its final response to a unit-function input is a non-zero constant.
(*c*) Its natural frequencies are relatively high.

Correspondingly,

(*a*) f(p) has no pole at the origin, $p = 0$.
(*b*) $f(0) = \text{constant} \neq 0$, because $f(0) = \lim_{p\to 0} f(p) = \lim_{p\to 0} p\bar{h}(p) = \lim_{t\to\infty} h(t) = \text{constant} \neq 0$.
(*c*) f(p) has no pole in the neighbourhood of the origin, $p = 0$.

Therefore, f(p) can be expressed by Maclaurin's series in the neighbourhood of the origin, $p = 0$. Consequently:

$$T = \lim_{t\to\infty} T(t) = \int_0^\infty [H(t) - h_n(t)]dt$$
$$= \lim_{p\to 0}\left[\frac{1}{p} - \frac{f(p)}{f(0)}\frac{1}{p}\right]$$

$$= \lim_{p\to 0}\left\{\frac{1}{p} - \frac{1}{\mathrm{f}(0)}[\mathrm{f}(0) + p\dot{\mathrm{f}}(0) + \ldots]\frac{1}{p}\right\}$$

$$= -\frac{\dot{\mathrm{f}}(0)}{\mathrm{f}(0)}$$

In other words, for a practical divider the time-displacement function is convergent, i.e. $\mathrm{T}(t)$ converges to T as t tends to infinity.

(7.2) Some Properties of the Ultimate Time-Displacement

Property (a).—If $\lim_{t\to\infty} v_i(t) = k$, k being a non-zero constant, then

$$T = \frac{1}{k}\int_0^\infty [v_i(t) - v_{on}(t)]dt$$

Proof.—It has been shown in the preceding section that

$$\lim_{p\to 0}\left[\frac{1}{p} - \frac{\mathrm{f}(p)}{\mathrm{f}(0)}\frac{1}{p}\right] = T$$

By assumption,

$$\lim_{t\to\infty} v_i(t) = \lim_{p\to 0} p\bar{v}_i(p) = k \neq 0$$

Thus,

$$T = \frac{1}{k}\lim_{p\to 0} p\bar{v}_i(p) \lim_{p\to 0}\left[\frac{1}{p} - \frac{\mathrm{f}(p)}{\mathrm{f}(0)}\frac{1}{p}\right]$$

$$= \frac{1}{k}\lim_{p\to 0}\left[\bar{v}_i(p) - \frac{\mathrm{f}(p)}{\mathrm{f}(0)}\bar{v}_i(p)\right]$$

$$= \frac{1}{k}\int_0^\infty [v_i(t) - v_{on}(t)]dt$$

Property (b).—If $\lim_{t\to\infty} v_i(t) = k$, k being a non-zero constant, and $\int_0^\infty [\mathrm{H}(t) - v_i(t)/k]dt = K =$ constant, then

$$T = \int_0^\infty \left[\mathrm{H}(t) - \frac{v_{on}(t)}{k}\right]dt - \int_0^\infty \left[\mathrm{H}(t) - \frac{v_i(t)}{k}\right]dt$$

Proof.—From property (*a*)

$$T = \frac{1}{k}\lim_{p\to 0}\left[\bar{v}_i(p) - \frac{\mathrm{f}(p)}{\mathrm{f}(0)}\bar{v}_i(p)\right]$$

By assumption,

$$K = \lim_{p\to 0}\left[\frac{1}{p} - \frac{\bar{v}_i(p)}{k}\right]$$

Hence,

$$T + K = \lim_{p\to 0}\left[\frac{\bar{v}_i(p)}{k} - \frac{\mathrm{f}(p)}{k\mathrm{f}(0)}\bar{v}_i(p) + \frac{1}{p} - \frac{\bar{v}_i(p)}{k}\right]$$

$$= \lim_{p\to 0}\left[\frac{1}{p} - \frac{\mathrm{f}(p)}{k\mathrm{f}(0)}\bar{v}_i(p)\right]$$

$$= \int_0^\infty \left[\mathrm{H}(t) - \frac{v_{on}(t)}{k}\right]dt$$

The proof then follows.

Property (c).—When a divider consists of a cascaded chain of m elements without mutual coupling, and each element introduces negligible loading to its preceding elements, then the ultimate time-displacement of the whole divider is

$$T = \sum_{s=1}^{m} T_s$$

where T_s is the ultimate time-displacement of the sth element.

The proof can be obtained by writing out T_s formally according to property (*a*).

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AUTOMATIC CHECK SYNCHRONIZING EQUIPMENT USING STATIC RELAYING PRINCIPLES

By COLIN ADAMSON, D.Sc., M.Sc.(Eng.), Associate Member, and O-P. MOSLAND, M.Sc.Tech.

(*The paper was first received* 16*th August*, 1960, *and in revised form* 27*th January*, 1961.)

SUMMARY

The paper describes circuit arrangements for automatic check synchronizing equipment using static relaying principles which have already been used with success in the field of protective gear. Existing commercial equipment is not entirely static, nor is any attempt being made so far to utilize the properties of transistors in this particular field. It is shown in the paper that all the necessary functions of voltage regulation, phase and slip measurements can be carried out successfully using these elements and principles.

The paper includes representative test results and a summary of performance.

(1) INTRODUCTION

A number of synchronizing relays are manufactured commercially, most of which, including those of present United Kingdom manufacture, rely exclusively on dynamic relaying elements. Three notable exceptions are American,[1] Swedish[2] and German[3] equipments which employ some electronic and static rectifier elements. The case for relaying equipment without moving parts is very strong and has been made elsewhere;[4, 5] great interest is also being expressed at present in improved control and synchronizing equipment for substations and power stations. For these reasons it was decided to investigate the possibilities of applying transistors and other semiconductor components to the problems of an entirely static check synchronizing equipment, the results of which are described in the paper.

(2) GENERAL PRINCIPLES OF SYNCHRONIZING EQUIPMENT

In order that two sources, of high apparent-power ratings and low internal impedances, can safely be connected in parallel, certain limitations must be imposed on the phase and frequency of the incoming source, in order not to cause any disturbance or discontinuity of the power supply.

At the moment of paralleling, i.e. when the circuit-breaker connecting the incoming voltage source to the existing source of supply should complete the circuit, three criteria must exist:

(*a*) The difference in time phase between the incoming and running voltages must be small and decreasing with time.

(*b*) The difference in the frequency between the two voltage supplies must be small, e.g. a slip frequency of 0·5% of the supply source frequency.

(*c*) The difference in magnitude between the two voltages must be small. The permissible difference in any particular case is dependent to some extent on the characteristics of the incoming system and the main system which are to be paralleled; no generally applicable standard can be quoted, although 5% was adopted for the laboratory tests described.

With regard to criterion (*a*), any circuit-breaker needs a definite time, T_c, for operation, from the moment the closing signal is received until the breaker contacts have established the power circuit. This implies that the synchronizing or paralleling relay must produce a closing signal at a time T_c before the instant of paralleling. With regard to criterion (*b*), no operation should take place at slip frequencies above, say, 0·5% of the supply source frequency; e.g. in the test described, the equipment has been arranged to lock out positively at high slip frequencies. A voltage-unbalance blocking device should also be incorporated in accordance with condition (*c*), so that no operation can take place if the voltages are unbalanced by more than a given percentage; possible arrangements for effecting this are many and varied, but the most economic ones depend to some extent on the general circuit principles adopted for the check synchronizing equipment.

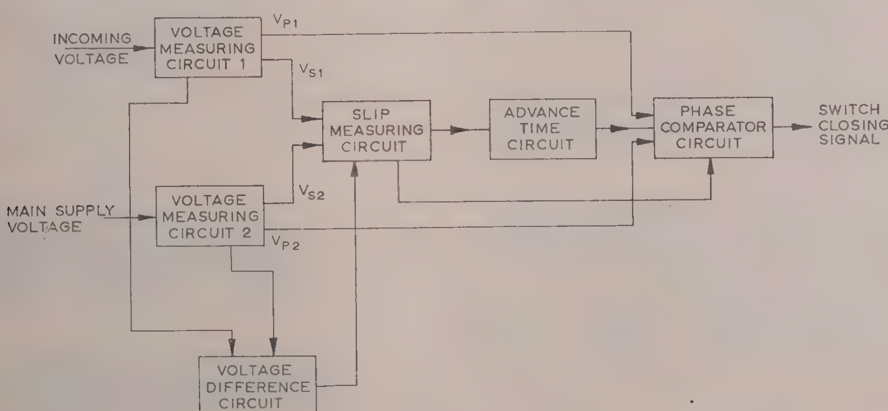

Fig. 1.—Basic block diagram of automatic check synchronizing equipment.

The overall requirements are thus as indicated in Fig. 1. The two voltage-measuring circuits each supply two outputs, V_{S1}, V_{P1} and V_{S2}, V_{P2}. V_{S1} and V_{S2} are applied to circuit arrangements which measure the slip, with the overriding

Written contributions on papers published without being read at meetings are invited for consideration with a view to publication.

Dr. Adamson and Mr. Mosland are at the Power Systems Laboratory, Department of Electrical Engineering, Manchester College of Science and Technology.

proviso that if the initial voltage difference is too great the slip-measuring arrangement will give no output (or be prevented from making any measurement). The outputs V_{P1} and V_{P2} are applied to phase-measuring circuit arrangements, with the overriding proviso that there will be no output (or no measurement) if the slip is too great. Control from the slip-measuring circuit is exerted through a time-advance arrangement which ensures that any output signal for closure of the circuit-making switch occurs at a time T_c before the precise instant of phase coincidence of the incoming and main supply voltages; T_c is thus the time from the issue of a closure signal from the check synchronizing equipment to the instant that the closing switch effectively makes the circuit.

(3) EXISTING PRACTICE USING ELECTRONIC OR OTHER STATIC ELEMENTS

In one of the synchronizing relays manufactured commercially,[1] the incoming and main (or reference) voltages are both derived by transformers with double secondary windings; one winding of each transformer is used with rectifier bridge circuits to generate a signal proportional to the slip frequency, and other windings are used for the indication of relative phase.

Provision is also made for determining the correct angle for commencing closure of the paralleling switch before current zero in the slip cycle.

This particular relay is unique in that it employs pentodes for signal detection and amplification. It has substantial disadvantages apart from this, in that it employs a number of telegraph relays for sequential operation; the sequential operating circuits are complex.

A second, commercially-available synchronizing relay[2] uses two voltage transformers, the secondary windings of which are connected in opposition to give an output which is the difference of the voltage signals from the main and incoming sources.

Two rectifier bridges are connected across this combined output so as to obtain the rectifier slip voltage and the derivative of the slip voltage. Both signals have a periodic frequency equal to that of the slip, the difference in frequency between the two source voltages. The amplitude of the slip signal is constant at all slip frequencies, and the amplitude of the derivative voltage varies with slip frequency. The slip voltage is applied to a series combination of a resistor and a capacitor acting as a differentiating circuit, the output voltage being taken across the resistor. The voltage drop across the resistor increases with increased slip.

These two voltages are applied to a polarized relay with two voltage coils in such a way that the relay will operate when the two voltages are equal. This occurs slightly in advance of phase coincidence. Owing to the variation in magnitude of the derivative voltage, the point of intersection between the two voltages will occur at a constant time prior to phase coincidence independent of slip frequency within certain limits of slip. The advance time is easily adjustable by controlling the magnitude of the derivative voltage.

A third rectifier circuit is arranged to act as a voltage comparator and will block the operation of the relay if the voltage unbalance is too large. The sequential control of the signals is arranged to block operation at high slip frequencies.

The third equipment incorporating some static relaying principles[3] employs two transformers for its input. These transformers each have two windings which are fed with currents (derived through resistors) from the incoming and running supplies; each transformer has a third, output winding. The winding arrangement is such that the two derived currents are subtracted from each other in one transformer and added in the other at the instant of phase opposition, the reverse being true for the instant of phase coincidence. Each of the outputs is then rectified to give two beat waveforms of the same crest value, which are then applied in opposition to the two input windings of a third transformer. This transformer has an air-gap in its core and thus gives an output proportional to the derivative of the input, i.e. proportional to the slip. The equipment incorporates two electromagnetic relays, one being polarized for slip-limiting purposes. The advance time, T_c, and the slip limit are both obtained by potentiometer settings incorporated as part of the phase-measuring circuit, which is supplied by the rectified outputs of the first two transformers.

(4) GENERAL FUNCTIONAL ARRANGEMENTS OF THE CHECK-SYNCHRONIZING RELAY EQUIPMENT

The conditions in Section 3 can all be met with great facility by the use of modern static relaying techniques; the necessary equipment consists essentially of four parts, each with a distinct function:

(*a*) Difference or slip-frequency measuring device.
(*b*) Phase indicator.
(*c*) Voltage comparator with built-in control of the permissible voltage variation.
(*d*) Sequential control circuits.

Part (*a*) is a combination of an electromagnetic circuit consisting of transformers and resistors, and rectifier bridges for signal rectification, together with a smoothing circuit.

Parts (*b*), (*c*) and (*d*) are all electronic transistor circuits of proved reliability; part (*c*) is achieved as an auxiliary function of one of the sequential control units.

(4.1) Slip-Measuring Circuit

The output voltages of the two sources to be synchronized are fed through voltage transformers into the slip-frequency-

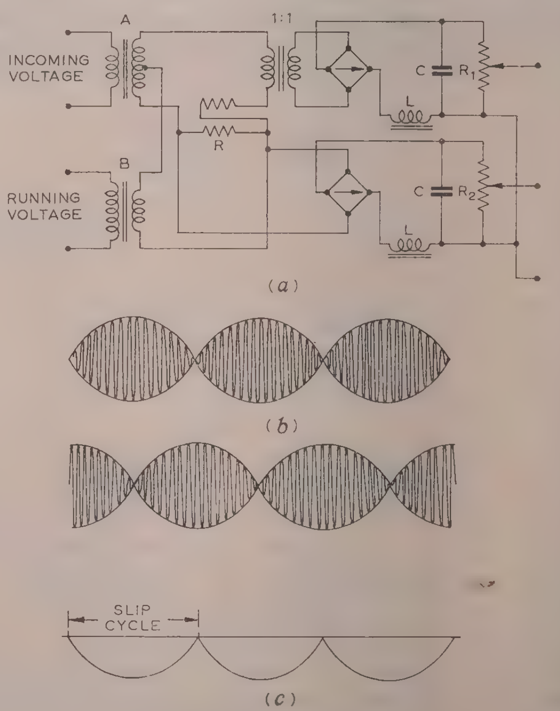

Fig. 2.—Circuit arrangements and waveforms of slip-frequency measuring device.

(*a*) General arrangement.
(*b*) Waveforms of signals across R and the 1 : 1 transformer for the case of $V_1 = V_2$.
(*c*) Rectified output waveshape available at R_1 and R_2.

measuring device, the output of which is a smooth rectified signal; the magnitude of this voltage is proportional to the phase displacement between the two input voltages, and the frequency is equal to the difference in frequency between the two voltages. This is illustrated in Fig. 2(*a*).

The slip-measuring device gives two similar output waveforms with a phase displacement of 180° between them. One of the slip signals will have its zero voltage at the point of phase coincidence and its maximum value 180° out of phase with the instant of phase coincidence. The second slip signal will have its zero at a point 180° out of phase with point of phase coincidence and its maximum at the point of phase coincidence. These are indicated in Fig. 2(*b*).

Consideration of the second slip signal may be deferred and reference made only to the slip signal having its zero at the point of phase coincidence of the two-source voltage. This particular slip signal is fed to pulse circuit 1, shown in Fig. 3.

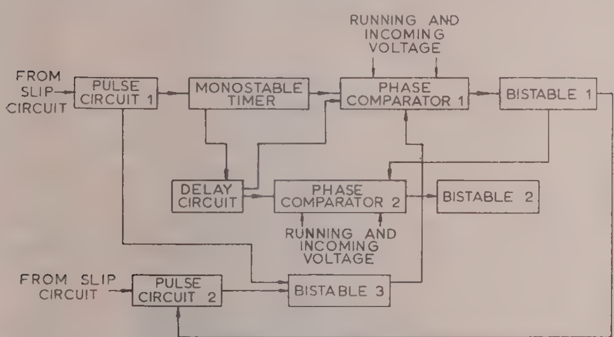

Fig. 3.—Block schematic of static synchronizing relay.

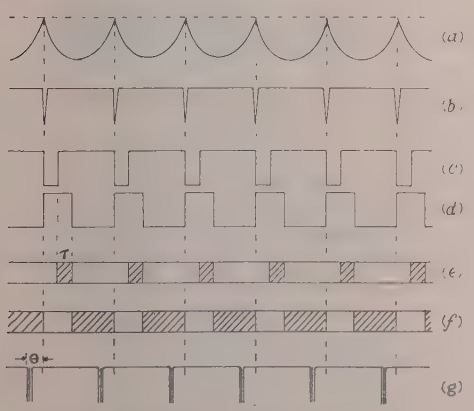

Fig. 4.—Sequential operation of static synchronizing relay.

(*a*) Output from slip-measuring circuit.
(*b*) Output from pulse circuit 1.
(*c*) Output from monostable timing circuit.
(*d*) Output from delay circuit.
(*e*) Operating period of phase comparator 1.
(*f*) Operating period of phase comparator 2 if bistable circuit 2 picks up.
(*g*) Final tripping signal at specified angle.

Its performance is such that, whenever the input signal from the slip-measuring circuit becomes zero—a condition which lasts for a very short period of time—a sharp negative pulse is produced, as shown in Figs. 4(*a*) and (*b*).

From Fig. 3 it may be seen that the signal from the pulse circuit is fed to a monostable timing circuit. This is designed to give a negative-going output square wave of duration equal to the time of operation, T_c, of the circuit-making switch. The pulse duration is variable to suit different types of circuit-breakers. The output of this stage is of the form shown in Fig. 4(*c*).

(4.2) Phase Indication

Two signals are derived from the monostable timing circuit, one of which is fed into phase comparator 1 and the other into the delay circuit.

The delay circuit gives a constant delay time subsequent to decay of the monostable pulse as shown in Fig. 4(*d*). The delay output from this circuit is fed to the two phase comparators.

From Fig. 3 it can be seen that the output of phase comparator 1 is controlled by five signals. It is designed to give zero output voltage provided that any one of these five signals is negative. The output signals of the monostable timing and delay circuit are so arranged that they permit phase-comparator operation only during the limited period τ between the trailing edges of their respective pulses, as shown in Fig. 4(*c*); this implies that the voltage waveforms of the running supply and incoming supply can only be compared in phase, in phase comparator 1, during this restricted period. Should the running and incoming voltages be in phase during the time τ, a negative-going signal is produced which is fed to a level detector through an integrating circuit within the phase comparator. The function of the integrator and level-detector circuits is to ensure that only if the two source signals are in phase during the whole of operating time of the phase comparator will an output be produced to operate bistable circuit 1, as indicated in Fig. 3.

As mentioned previously, phase coincidence of the two signals which are to be synchronized takes place on numerous occasions. However, interest lies only in the instant of phase coincidence, when the slip frequency is extremely small. Thus, by limiting the phase-comparator operating time and ensuring that operation of the level detector takes place only when the two signals are in phase, during the whole of this specified time there is a guarantee of operation at slow slip frequencies only.

(4.3) Production of Tripping Signal at the Correct Instant before Phase Coincidence

If a signal is available to switch on bistable circuit 1, this circuit, if operated together with the delay circuit, will in its turn switch on phase comparator 2, which will again measure the phase angle between the two source voltages; when the phase difference attains an angle θ, corresponding to the time delay T_c of the monostable timing circuit, a signal is produced to trigger bistable circuit 3, which controls the circuit-breaker operating mechanism at the correct instant [Fig. (3)].

The above discussions apply and give proper results when the slip frequency is sufficiently low, such that at the end of the monostable-circuit preset time the vector V_1, in its rotation with respect to vector V_2, does not come within zone 2, as indicated in Fig. 5. Such a limitation is not desirable since it makes the

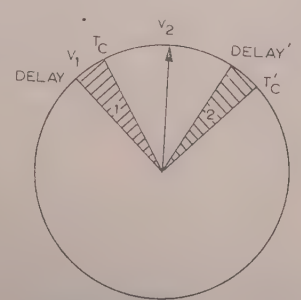

Fig. 5.—Correct conditions for synchronization.

system operate properly only when the slip frequency is too low. To overcome this difficulty, two of the circuits shown in Fig. 3, namely pulse circuit 2 and bistable circuit 3, have been added. Pulse circuit 2 produces pulses when V_1 and V_2 are in anti-phase. Bistable circuit 3 is set by pulse circuit 2 and reset by pulse circuit 1. If the slip frequency is close to synchronism and phase comparator 1 picks up at the preset angle θ in the first half-cycle, correct operation results with pulse circuit 2 and bistable circuit 3 both maintained inoperative by a signal from bistable circuit 1.

On the other hand, if operation is not obtained in the first half of the slip cycle, the system will be maintained inoperative during the second half-cycle by a signal from bistable circuit 3.

At the end of the cycle the system is reset by pulse circuit 1, to start the sequence of operations all over again.

(5) CIRCUIT ARRANGEMENTS

(5.1) Electromagnetic Slip-Frequency Measuring Circuit

The electromagnetic slip-frequency measuring circuit is designed to give a sinusoidal output waveform, the frequency of which will be the difference between the frequencies of the two applied voltages.

Referring to Fig. 2(*a*), the running and incoming voltages are connected to the primary windings of the two transformers A and B; their secondary windings are so connected as to add and subtract the two voltages, the outputs being taken across the two rectifier bridges. The secondary winding of transformer A is centre-tapped, and the central point is connected to one side of the secondary winding of transformer B. The voltage applied across each half of the secondary winding of transformer A is equal to that across the secondary winding of transformer B, when the two input voltages on the primary windings of the two transformers are equal.

Consider the two voltage waveforms:

Running supply voltage, $V_R = V_1 \sin \omega_1 t$
Incoming voltage, $V_G = V_2 \sin \omega_2 t$

Assuming $V_1 = V_2 = V$, a condition which must be satisfied within permissible limits and without which no synchronizing signal can be issued,

$$V' = V_R + V_G = V(\sin \omega_1 t + \sin \omega_2 t)$$

$$= 2V \sin \frac{\omega_1 + \omega_2}{2} t \cos \frac{\omega_1 - \omega_2}{2} t \quad . \quad (1)$$

and

$$V'' = V_R - V_G = V(\sin \omega_1 t - \sin \omega_2 t)$$

$$= 2V \cos \frac{\omega_1 + \omega_2}{2} t \sin \frac{\omega_1 - \omega_2}{2} t \quad . \quad (2)$$

Eqns. (1) and (2) give rise to waveforms of the shape shown in Fig. 2(*b*). The period of oscillation of these two waveform envelopes is proportional to $\omega_1 - \omega_2$, the slip frequency.

Considering the voltage wave $V'' = V_R - V_G$, from Fig. 6, the resultant voltage V'' will be proportional to the phase difference between the two signals.

When $V_1 = V_2 = V$

$$V_R - V_G = 2V \sin \alpha/2 \quad . \quad . \quad . \quad . \quad . \quad (3)$$

Eqn. (3) indicates that, when the two signals are in phase and of the same voltage magnitude, the resultant voltage is zero, and it also indicates that V'' is proportional to the sine of the phase displacement.

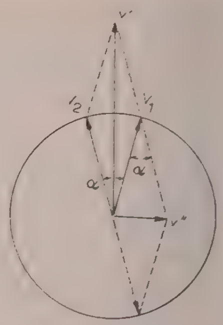

Fig. 6.—Summation and subtraction of voltages.

If $V_1 \neq V_2$ the sine wave will have a d.c. component, and V'' will not attain zero potential as shown in Fig. 7, and we have

$$V'' = V_R - V_G = \sqrt{(V_1^2 + V_2^2 - 2V_1V_2 \cos \alpha)} \quad . \quad (4)$$

When $V_1 = V_2$

$$(V_R - V_G)^2 = 2V_1^2(1 \cos \alpha)$$

$$V'' = (V_R - V_G) = \sqrt{2}V_1\sqrt{(1 - \cos \alpha)} \quad . \quad . \quad (5)$$

This agrees with the previous equations since $\sin^2\alpha = \frac{1}{2}(1 - \cos 2\alpha)$.

The same argument applies to eqn. (1), only this gives a cosine wave of the same slip frequency, thus having its zero potential

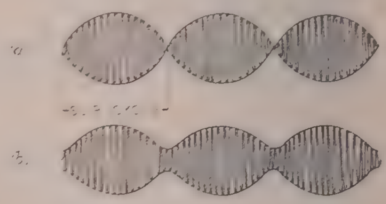

Fig. 7.—Slip waveforms prior to rectification.
(*a*) $V_1 = V_2$.
(*b*) $V_1 \neq V_2$.

at 180 electrical degrees displacement (the two voltages are in anti-phase) and its maximum at phase coincidence.

The two waveforms given by eqns. (1) and (2) are rectified to give negative half-cycle output; this wave is smoothed and freed from ripples in an *LC* filter circuit. The output waveform appearing across the two potentiometers, R_1 and R_2, is of the form shown in Fig. 2(*c*). These two waveforms are fed to the transistor circuits described in Section 5.

A bridge connection is used to avoid feedback between the two voltages. The 1 : 1 transformer in one arm of the bridge is used to make it possible for the rectified output signals to have a common point.

(5.2) Transistor Circuits (see References 6–9)

(5.2.1) Pulse Trigger Circuit.

The pulse trigger circuit is a very simple common-emitter circuit, consisting of one transistor only, the input signal of which is the slip voltage. Provided that the input signal of the base connection is negative with respect to the emitter voltage, the transistor will be in a conducting state and the output voltage at the collector will have the same potential as the emitter If the signal at the base connection is equal to, or positive with respect to, the emitter potential, the transistor will cut off and the voltage of the collector will drop. Thus, for the short instant

when the slip voltage fed to the pulse trigger circuit is zero, a sharp negative pulse is derived at the collector output terminal.

Should the slip voltage have a d.c. component, i.e. should the two applied voltages not be equal in magnitude, the slip voltage will not attain zero and no output pulse will be derived. This ensures that no operation takes place unless the two applied voltages are of the same magnitude.

(5.2.2) **Monostable Timing Circuit.**

In a monostable trigger circuit, a triggering signal is required to induce a transition from its stable to its quasi-stable state. The circuit may remain in the quasi-stable state for a time which is very long compared with the time of transition between the two stages. Eventually, however, the circuit will return from its quasi-stable to its stable state, no external signal being required to induce this reverse transition. The monostable trigger circuit used in this relay is a common-emitter type as shown in Fig. 8.

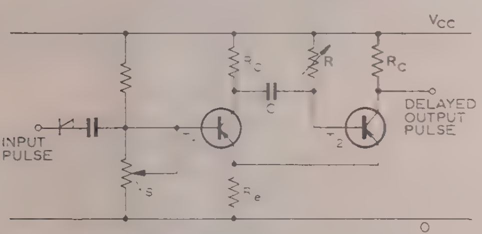

Fig. 8.—Monostable timing circuit.

In the stable state, transistor T_2 is conducting because its base is taking current from the negative supply, transistor T_1 being biased to cut-off. When the trigger signal at the base of T_1 causes the transition from the stable to the quasi-stable state, T_2 cuts off, T_1 starts conducting, and an emitter current I_1 flows in T_1. This current is dependent on the setting of V_s.

Immediately after the trigger pulse has been applied, the collector voltage of T_2 falls abruptly. After the initial abrupt transition, the base voltage of T_2 starts to fall exponentially towards V_{cc}, all the other voltages remaining constant until the base voltage of T_2 falls to the pick-up voltage. At this point the quasi-stable state is terminated, and the voltage finally stabilizes to its quiescent level, I_2R_e.

The circuit thus produces a negative pulse at the collector of T_2 and a positive pulse at the collector of T_1, each with a duration determined by the time-constant of the RC coupling and the voltage V_s.

In the stable state T_2 is conducting and T_1 is cut off. The d.c. bias potential is such that T_1 is nominally cut off.

The trigger duration t_1 is determined from the relationship

$$t_1 = RC \log_\varepsilon \frac{2V_{cc} - V_{e1} - V_{e2}}{V_{cc} - V_{e1} - V_n} \quad . \quad . \quad . \quad (6)$$

Where the suffix e denotes an emitter and V_n is the base voltage in the transistor characteristic.

(5.2.3) **Emitter-coupled Trigger Circuit employed for Delay and Phase Comparator Circuits.**

The circuit of Fig. 9 has characteristics which strongly recommend its use as a level detector in relaying applications. It remains in one stable state so long as its input voltage is lower than a certain value which is the pick-up voltage V_p. As soon as the input voltage reaches this value, the circuit suddenly changes to another stable state, and the output appears as a sudden change in voltage level.

The circuit consists of two transistors, their emitters being

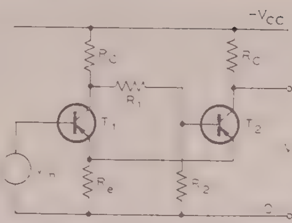

Fig. 9.—Emitter-coupled trigger circuit.

connected through a common emitter resistance R_e to the zero-voltage line.

When V_{in} is lower than the pick-up voltage V_p, T_2 in Fig. 9 is fully conducting owing to the negative voltage applied to its base through the voltage divider R_c, R_1 and R_2, whilst T_1 is biased to cut-off by the voltage drop across the common emitter resistance R_e. When V_{in} reaches the pick-up value, T_1 starts to conduct and its collector voltage starts to rise. This rise is transferred to the base of T_2 through the voltage divider R_1 and R_2, and therefore reduces its emitter and collector currents.

Reduction in I_{c2} pushes T_1 further to conduction with a consequent rise in its collector voltage which is again transferred to the base of T_2, and the cycle is repeated. If during this action the loop gain is greater than unity, the circuit will abruptly change to the other stage of stability, where T_1 is fully conducting and T_2 is cut-off, without any need for V_{in} to increase further beyond V_p.

When the input voltage is reduced, the circuit does not return to its initial state at the same voltage V_p, but will do so at a lower drop-out voltage V_d. This is due to the fact that, in order to trigger the circuit back to its original state, it is not sufficient to reduce the current passing through T_1 but necessary also for T_2 to start conducting.

Now $$V_p = I_{e2}R_e \frac{V_{cc}R_e}{R_c + R_e'} \quad . \quad . \quad . \quad . \quad . \quad (7)$$

and $$V_d = I_{c1}R_e = \frac{aV_{cc}R_e}{aR_c + R_e} \quad . \quad . \quad . \quad . \quad (8)$$

Eqns. (7) and (8) show that V_d is always less than V_p, since a is always less than unity.

The circuit of Fig. 9 can be arranged to reduce the difference between V_p and V_d or make them equal if necessary; this can be done by inserting a resistance in series with the emitter of T_1 or T_2.

(5.2.4) **Time-delay Circuit.**

The time-delay circuit, which is shown in Fig. 10, consists of a level detector shunted at its input by a capacitor C_d, and succeeded by an output stage. When the starting signal is applied

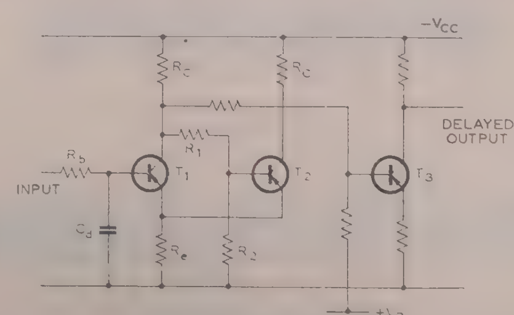

Fig. 10.—Time-delay circuit.

to its input, a delayed step function going from zero voltage to $-V_{cc}$ is produced at the collector of T_3 in Fig. 10, and a pulse with the same delay but stepping from $-V_{cc}$ to zero is produced at the collector of T_2.

The time delay is given by

$$t_d = \frac{R_{s2}C_dR_d}{R_{s1} + R_{s2}} \quad . \quad . \quad . \quad . \quad . \quad . \quad (9)$$

(5.2.5) **Phase Comparator.**

The phase comparator is the main relaying element in the synchronizing relay. It consists of a coincidence stage controlled by pulse circuits and the two source voltages to be compared in phase; it is followed by an integrating circuit and a level-detector output stage, as shown in Fig. 11(*a*).

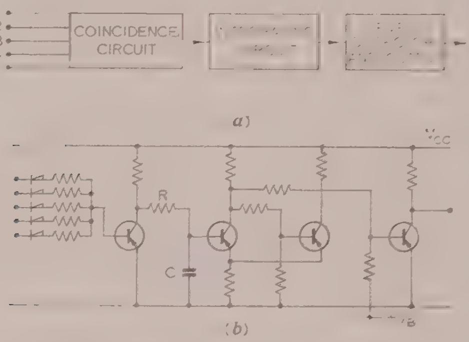

Fig. 11.—Multiple-input phase comparator.
(*a*) Block diagram of functions.
(*b*) Circuit arrangement.

The coincidence circuit will only allow operation if all its input signals are zero or positive at the same time. Should any of the input signals be negative no operation will take place since the collector voltage of the circuit will be at zero voltage, i.e. the transistor will be conducting.

The integrating circuit is a straightforward arrangement of a capacitor charging through a resistor during intervals when the coincidence transistor is cut off, but a linear integrator circuit may be used for greater accuracy as in other relaying applications.[9]

5.2.6) **Asymmetrical Bistable Trigger Circuit.**

The asymmetrical bistable trigger circuit (bistable circuits 1 and 2) is very useful as a slave element in relaying operations. It has the characteristic of being capable of responding to an input pulse or pulses, and producing a continuous output signal which may be used to control the tripping coil of a circuit-breaker.

As shown in Fig. 12, the circuit consists of two transistors T_1

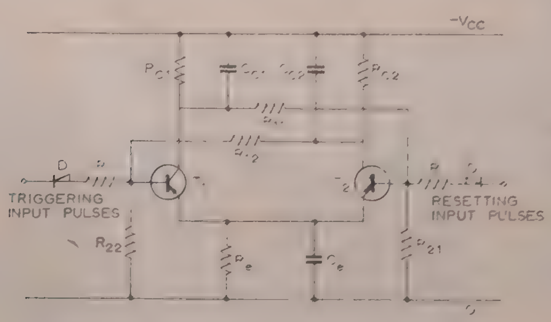

Fig. 12.—Asymmetrical bistable trigger circuit.

and T_2 with their emitters connected to the zero-voltage line through a common emitter resistance R_e shunted by a capacitor C_e. The signal at the collector of each transistor is fed to the base of the other transistor through coupling attenuators R_{11}, R_{21} and R_{12}, R_{22}. This circuit has two states of stability, in either of which one transistor is fully conducting and the other is cut off, and can remain in one of these stages indefinitely unless it is forced to the other state by some internal means. The capacitors shown in the Figure may be stray or intentionally connected. They determine the behaviour of the circuit during the transition from one stable state to another.

The circuit can be triggered by a negative pulse applied to the base of T_1 through a diode D in series with a capacitance C to cancel any d.c. component, as shown in Fig. 13.

If, after the circuit has been triggered, the h.t. supply is switched off and then on again, the circuit will assume its original state of stability with T_2 fully conducting and T_1 cut off; it remains in this state until again triggered to the other state.

This method of resetting is very useful in relaying applications, where the operation indicator can be used for this purpose.

(5.3) Power Supplies

The power-supply requirements for the relay are a negative direct voltage of 10 V capable of delivering up to 50 mA, and a 6 V positive bias of very little current drain. The bias voltage, although it can be provided by the power unit which supplies the main h.t. voltage, was supplied by a small battery since its current is very small and the voltage level required is not critical.

Transistor-regulated power-supply units are now widely available and are very suitable.[10]

(6) COMPLETE CIRCUIT AND CONSTRUCTION ARRANGEMENTS

The complete circuit arrangement is shown in Fig. 13. In order to simplify the wiring of the electronic circuits it was found most advantageous to use printed-circuit techniques, one printed-circuit board being allocated to each functional element. This presented no difficulties owing to the simplicity of the standard transistor circuits; the circuits were sectionalized and made into plug-in units to suit standard plugs.

The whole relay with its electromagnetic and transistor circuits was built into one unit of size 16 in × 5½ in × 6 in, but could be made smaller.

(7) PERFORMANCE AND OPERATION

(7.1) Slip Limitations

The combination of the monostable, delay and phase comparator 1 circuits ensures that the relay does not start operating until the slip frequency has been reduced below 0·4 c/s, although other choices of slip-frequency limitation can be made as required. After this point has been reached, the incoming generator(s) have to make one complete revolution before the final closing signal to the circuit-breaker operating mechanism can be produced. The period of this slip cycle will thus have to be more than 2·5 sec for operation to take place, and the generator must pass slowly through synchronism.

If, for any reason, the circuit-making switch failed to close at sub-synchronous speed, a safeguard is required to ensure that no operation takes place beyond 0·4 c/s above synchronous speed. In order to achieve this a resetting signal was added to bistable circuit 1 at the end of each slip cycle, the arrangement being made ready again by the pulse from phase comparator 1. This ensured that the relay would operate only between the chosen limits of $\pm 0{\cdot}4$ c/s slip.

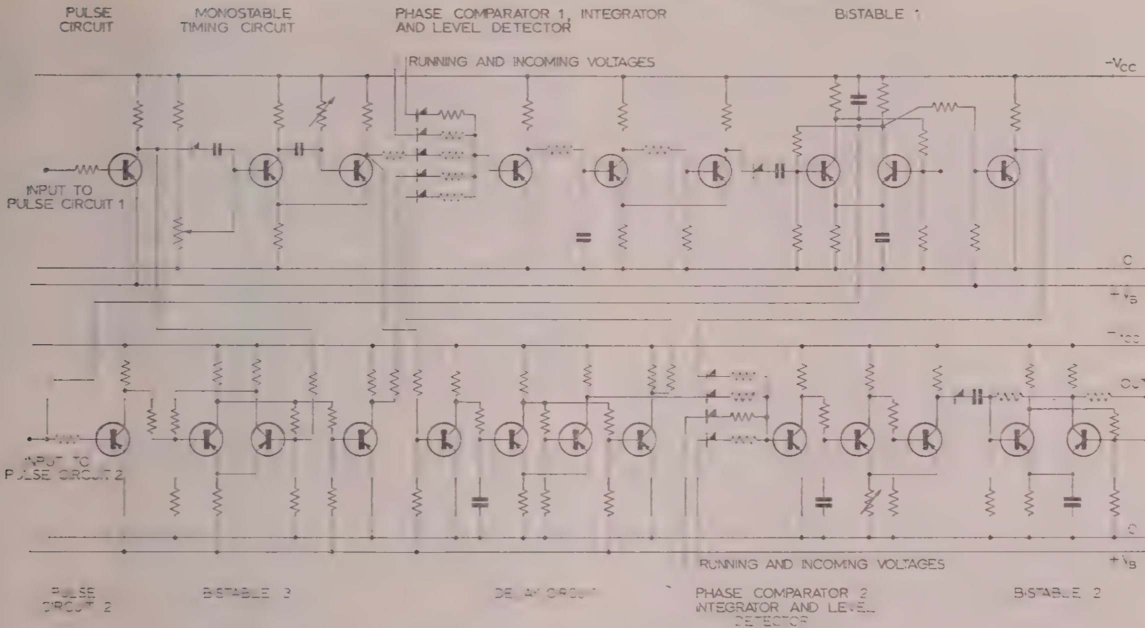

Fig. 13.—Complete circuit of automatic check synchronizing relay.

(7.2) Unbalance in Source Voltages

The effect of unbalance in source voltages was that, with 5% unbalance, pulse circuit 1 was not able to trigger the monostable circuit, and thereby prevented operation; this feature is due to the slip cycle not attaining zero voltage with unbalance in the source voltages. The limit can be reduced slightly by connecting a small resistance in the emitter circuit of the pulse circuit, thus reducing the cut-off voltage.

(7.3) Advance Time Setting

The advance time setting is not completely independent of the slip. If the advance-time calibration should be set to give a correct value at 0·3 c/s, the circuit-making switch would close slightly after phase coincidence of the two source signals between 0·4 and 0·3 c/s slip frequency, and slightly in advance for slip frequencies between 0·3 c/s and zero. Within these limits of slip the divergence from the calibrated value of time is so small as to cause no disturbance and could be neglected.

(7.4) Temperature Considerations

The transistor is a very temperature-sensitive device and large changes in junction temperature will change its characteristics, although previous work has demonstrated its continued reliability with temperature change in circuit configurations which confine it to a switching role. Since the collector power dissipation in switching circuits is negligible, the junction temperature will be very nearly the same as the ambient temperature. The relay was found to work satisfactorily between 10°C and 30°C. Outside these limits temperature compensation must be applied to the affected transistor circuits, or less temperature-sensitive transistors, e.g. silicon transistors, must be employed.

(7.5) General Test Arrangements

In the initial stages of construction and testing, the synchronizing relay was tested by simulating the incoming generator by a source variable in both voltage and frequency, the reference source being the mains supply. The equipment is designed to operate on two phases of a 3-phase system and can thus be applied to and tested on a single-phase system; after completion the equipment was tested by synchronizing experiments on a single-phase generator driven by a variable-speed motor. In these tests, manual control was exercised over the excitation current of the generator; in service, an automatic voltage regulating relay would be employed for the field of the incoming machine as proposed in the Appendix 11.

(7.6) Oscillograph Records

The most instructive records from these tests are of the slip voltage, which indicates whether the equipment is operating at or below the predetermined slip frequency, and of the instant of occurrence of the closing signal to the circuit-making switch which should appear in the correct phase. Typical oscillograms illustrating these points are shown in Figs. 14, 14 (continued) and 15.

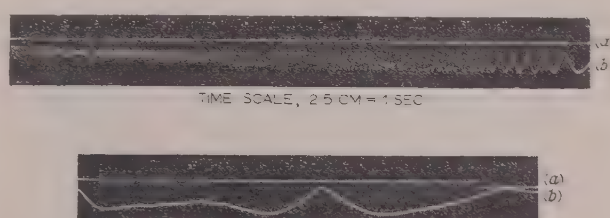

Fig. 14.—(*a*) Advance-time signal for operation of closing switch.
(*b*) Slip waveform.

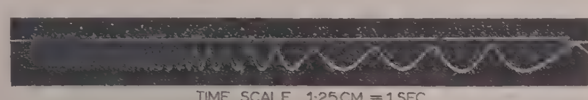

Fig. 15.—(*a*) Advance-time signal for operation of closing switch.
(*b*) Slip waveform.

In Fig. 14, the traces are for a case where all the conditions for correct synchronizing have been met and the circuit-making switch is being closed; trace (*b*) shows the slip voltage, the period of which is increasing with time, whilst trace (*a*) shows the closing signal occurring when the slip falls to the predetermining frequency and in the correct phase to allow for the predetermined time of operation of the closing switch. In Fig. 15 the oscillograms show the incoming machine being taken through synchronism, the closing signal being obtained but the circuit-making switch not being closed. In both cases the advance time setting is 250 ms; the time scale for Fig. 14 is 2·5 cm/s and for Fig. 15 it is 1·25 cm/s. The closing pulse has occurred at 0·35 and 0·4 c/s in the cases of Figs. 14 and 15, respectively.

(8) CONCLUSIONS

The work carried out on the automatic transistorized check-synchronizing relay has strengthened the view that junction-transistor circuits can be successfully and reliably applied to relaying and control problems, especially where more complicated relaying circuits are concerned. The relaying equipment described in the paper consists solely of standard transistor pulse circuits, which should simplify any manufacturing procedure. The transistor circuits will give reliable and consistent operation and have all the advantages gained by having no moving parts.

The relay has been shown to impose an extremely low burden on the voltage transformers of the system to which it is connected. Although power supply units should be provided, this does not present any great problem since a simple transistor power-supply unit energized from the mains supply or voltage transformers can be incorporated, and the biasing voltage can be obtained from long-life cells or from the power unit. The power drain of the equipment is 35 mA at 10 volts from the main power unit and 5 mA at 6 volts for bias supplies. There is no reason why this low burden should not permit energization of the power supply unit from the voltage transformers.

The relay advance-time setting was chosen to deliberately wide limits to cover the range 100–450 ms, which relates to making-switch closing times of 5–12·5 c/s at 50 c/s. It should be remembered that the advance time setting includes the time from the instant when the circuit-breaker operating mechanism becomes energized to the successful completion of the electrical power circuit. Since the switch generally has to close against a heavily-loaded release spring, a minimum closing time of 5 cycles should cover the oil circuit-breakers in use. With high-speed switches for system interconnection this value might be rather high, but it can readily be adjusted if required.

The accuracy of the determination of the advance time setting has been mentioned. The relay was calibrated to give the correct advance time at 0·3 c/s slip. Should the relay operate at a lower slip, a slight inaccuracy in time setting will be encountered; in any case the out-of-phase switching angle will not exceed 5°, within the operational limits of slip. This deviation will be of practically no consequence, and no instability or damage to the incoming machine[3] will be caused. On synchronizing large generators, either turbo- or water-wheel, the run-up should be very smooth owing to their large inertia and accurate speed-matching control, thereby creating optimum conditions for approaching synchronism and operation of, the synchronizing equipment at 0·4 c/s slip, or lower as required.

For special purposes, the maximum slip can be decreased or increased within certain limits. The maximum slip setting for operation should not exceed 0·75–1 c/s even with the most accurate time setting.

The limit of voltage unbalance of 5% does not seem excessive compared with figures of 10% quoted for most of the relays described in Section 3. If the check-synchronizing relay is incorporated as part of a complete automatic synchronizing scheme, a voltage controlling device should be incorporated which will keep this unbalance to a much lower value; consideration is given to this feature in Appendix 11. The relay can be made a 'fail-safe' device by incorporating a voltage-unbalance indicator and check feature.

The automatic check-synchronizing equipment is only a minor application in the rapidly growing use of automation in the power-generating and supply industry. Modern interconnected Grid systems with numerous supply and switching stations introduce operating problems of such complicated character that it is undesirable to rely solely on human factors. This, together with the important factor of economy of operation, has greatly speeded up the displacement of human personnel by automatic control and protection—a process which is likely to be accelerated in the future.

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(11) APPENDIX

Static Voltage-Regulating Relay

If voltage-control features are to be incorporated in the static synchronizing equipment described, further static relaying elements are necessary. In general, a static relay may be specified which can regulate a.c. or d.c. circuits, as appropriate, by controlling transformer on-load tap-change apparatus, resistance banks or rheostats, induction regulators, etc. Such a relay may be applied to control the field circuit of an incoming machine to be synchronized and should have the following minimum general specification:

(*a*) *3-state output arrangement.*—This provides for 'lower', 'normal' and 'raise' control of the incoming generator field circuit. 'Normal' will be the state of the output device when the field-circuit current produces an open-circuit voltage at the terminals of the incoming machine which corresponds to that of the main busbars.

(*b*) *Voltage-fail element*.—This disconnects the regulating output arrangement in (*a*) should the supply to the voltage-regulating relay fail. Any one of three possible modes of operation may follow as a consequence of failure of relay supply. The field regulator can be arranged to operate at its nominal level corresponding to 'normal' operation of the voltage-regulating relay, or the field regulator can be left at the level it occupied immediately prior to relay supply failure, or a signal can be made available which will prevent operation of the synchronizing relay.

(*c*) *Time-delay element*.—This will control the response of the main field-regulating arrangement to the output signal obtained from (*a*).

Fig. 16 shows the functional arrangement of a device to meet the general specification. The input signal, in this case a fraction of the terminal voltage of the incoming machine, is fed via a time-delay circuit to two level detectors, each consisting of two transistors and associated resistors. One level detector, say 1, is set whilst the other is reset when the voltage is low by a predetermined percentage, and vice versa. If the voltage is normal both level detectors are reset. The output from each of the level detectors, 1 and 2, is fed, respectively, to a transistor with a collector load consisting of one coil of a 2-coil relay, the moving contact of which has three positions as shown. A third level detector, 3, gives an output when the supply voltage to the regulating relay fails; this is arranged by supplying a reference voltage from a Zener diode connected in series with resistors across the negative d.c. supply of the regulating relay. Level detector 3 gives an output when the regulating-relay supply voltage is normal and maintains the output device, consisting of the two transistors with relay-coil loads, in a switched-on state. If this same supply voltage fails, level detector 3 resets

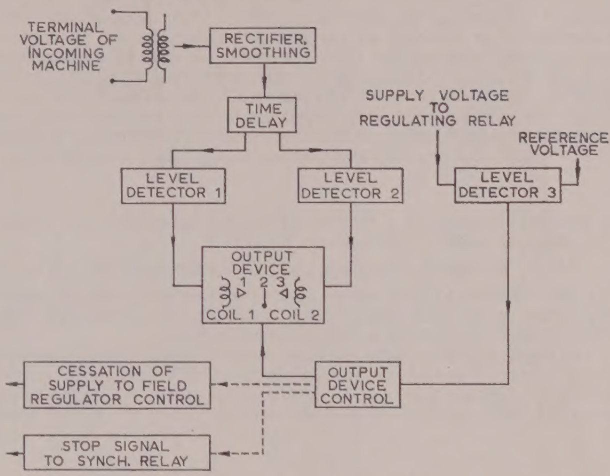

Fig. 16.—Functional arrangement of a static voltage-regulating relay.

and switches off the output device, thereby ensuring that the output-relay moving contact occupies the mid-position 2. This would normally lead to the first eventuality in (*b*). Two other possible outputs from the output-device control are shown, one to cause the main field-regulating control to remain in its last position in the event of supply failure to the regulating relay, and the other to provide a stop signal to the synchronizing relay by applying a negative signal to an additional sixth input in the base circuit of the phase-comparator input transistor (Fig. 13).

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Analytical Determination of the Characteristics of Enclosed and Oil-Immersed Fuses. Monograph No. 449 S.

COLIN ADAMSON, D.Sc., M.Sc.(Eng.), and M. VISESHAKUL, M.Sc.Tech., Ph.D.

In a previous paper the authors dealt with the case of semi-enclosed fuses, manufactured from uniform thin strip or with single or double discontinuities in their cross-sections. An analytical solution for the time/current characteristic of such fuses was shown to exist, which was in very close correlation with experimental derivations of the same characteristics.

This work has now been extended in two ways. First, the analytical derivation of fuse characteristic has been extended to cases of fuses with n discontinuities in their cross-sections where $n > 2$; this work was carried out with the aim of investigating the full range of alternative characteristics available from such fuses. Secondly, and the most important part of the work, has been the extension of the solution obtained for a semi-enclosed fuse to fuses of similar metallic construction but immersed in oil or enclosed in a silica-type filler.

Having established a method and form of solution, a substantial amount of computation is necessary in any one case; in order to facilitate computation, a simple form of transient analogue computer has been used, and is described in the paper. In the cases of fuses immersed in oil or enclosed in silica-type filler, the main problems have been to establish the parameters of heat transfer prior to finding a form of solution of the relevant equations.

The appendixes contain representative tables of results in the form in which they were obtained from the analogue computer, and an analysis showing the feasibility of establishing a single series of tables of analogue-computer results for fuses with multiple discontinuities in cross-section.

A Theoretical and Analogue Approach to Stray Eddy-Current Loss in Laminated Magnetic Cores. Monograph No. 453 U.

D. A. JONES, B.Sc., Ph.D., and W. S. LEUNG, B.Sc.(Eng.), Ph.D.

A method of obtaining the interlaminar eddy-current loss in laminated magnetic cores is devised by using a resistance-network analogy. It is assumed that in a laminated-core section the flux distribution is uniform, that there is a uniform resistivity in the direction across the laminations and that the IR drops in the direction along the planes of the laminations are negligible compared with those in the perpendicular direction. The stray eddy-current loss is derived as a function of the resistivity ratio between the two directions. The network analogy leads to the subsequent estimation of the effect on core loss of any short-circuiting paths in the section.

An experimental technique was developed to investigate the eddy-

current distribution in any conductive network through which a uniform alternating magnetic flux passes. The eddy-current distribution for an isotropic section was first obtained. An analogy to the laminated-core section from the point of view of conductivity was effected using a differential wire network whose resistivity in one direction was of the order of 10^4 times that in the perpendicular direction. The eddy-current distribution in such a network under an alternating magnetic field was found to confirm the theoretical calculations for core sections with and without interlaminar short-circuits.

A Hunting Analysis of a Permanent-Magnet Alternator and a Synchronous Motor. Monograph No. 454 S.

M. H. Walshaw, B.Sc.(Eng.), and J. W. Lynn, M.Sc., Ph.D.

Little has hitherto been written about the transient analysis of interconnected salient-pole synchronous machines of comparable size, where infinite busbars cannot be assumed to exist.

The paper describes theoretical and practical work carried out on a simple power system of this type, in which a 60 kW permanent-magnet alternator supplies power to a synchronous motor. For analytical purposes the permanent-magnet alternator is shown to be equivalent to a conventional alternator with a constant field voltage. The measurement of the parameters of the machines is described and the steady-state performance of the interconnected system is predicted by Kron's method of analysis.

The hunting equations of the system are examined, and a frequency-response method involving Nyquist's stability criterion is used to predetermine the effects of armature resistance on the hunting stability of the system under various conditions of load and saturation. A digital computer is used to perform the calculations and the predicted results are found to agree well with those obtained experimentally from the actual system.

Electromagnetic Forces in Slotted Structures. Monograph No. 456 U.

Prof. F. C. Williams, C.B.E., D.Phil., D.Sc., F.R.S., and R. S. Mamak, Ph.D.

The paper suggests a way of explaining the mechanism of force production in slotted structures which leads into a method of determining the precise location of these forces.

The case of the rounded corner slot is also studied, and by the application of numerical techniques the fluxes at the boundary are integrated to study the force distribution in depth.

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